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Hydrogen and Nitrogen as Causes of Gassiness in Ferrous Castings

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Abstract

Gassiness in iron and steel is often a function of the hydrogen content and the changes in solubility of hydrogen in metal which take place during solidification. Hydrogen is introduced into the liquid metal by means of carriers, the most common of which is moisture. Bubbles of hydrogen can form only when nucleating centers are present. These are generally supplied by hydrogen-oxygen or other reactions. Austenitic steel is less subject to gassiness and bleeding than ferritic steel. This is true because the solubility of hydrogen in the solid metal is greater, and the gamma lattice-structure range, in which hydrogen solubility is high, is extended. The addition of deoxidizers serves to replace some of the hydrogen which would otherwise act as a deoxidizer, thus tending to increase the concentration of hydrogen gas in the metal. In exceptional cases, gassiness and bleeding may result from the release of nitrogen by unstable nitrides present in the metal. This occurrence may be prevented by the addition of stabilizing elements such as titanium.

Introduction

1. In that class of ferrous castings often referred to as "dead" metal, which includes cast iron, certain ferro-alloys, and killed steel, a bothersome gassiness sometimes develops late in the period of solidification. Externally, the phenomenon may show itself as "worms," "peas," "backing up," "bleeding," or "rising," to name but a few of the colloquial foundry expressions. Internally, a porosity shows itself which has a similarly wide range of nomenclature.

2. During the following discussion, no confusion should de-

velop if a single representative term—such as "bleeding" for the effusion of metal and "gassiness" for the gas holes—is selected from the variety of shop parlance. All the terms plainly refer to one specific phenomenon: *the rejection of gas during late stages of solidification in castings of supposedly "dead" metal.*

3.   In many cases, surface layers are penetrated at selected points by the ejected liquid, giving rise to the so-called "worms" and extrusions typical of "bleeding." Again, the riser, or the ingot top, may simply lift a bit, or "rise," because of the bloating within. As the discussion develops, it will become clear that all these colloquial observations are one, and that presumed minor differences are principally matters of degree only.

THREE GASIFYING ELEMENTS IN FERROUS METALS

4.   Figures 1 and 2 illustrate two arbitrarily chosen degrees of effusion which have become known more or less separately as
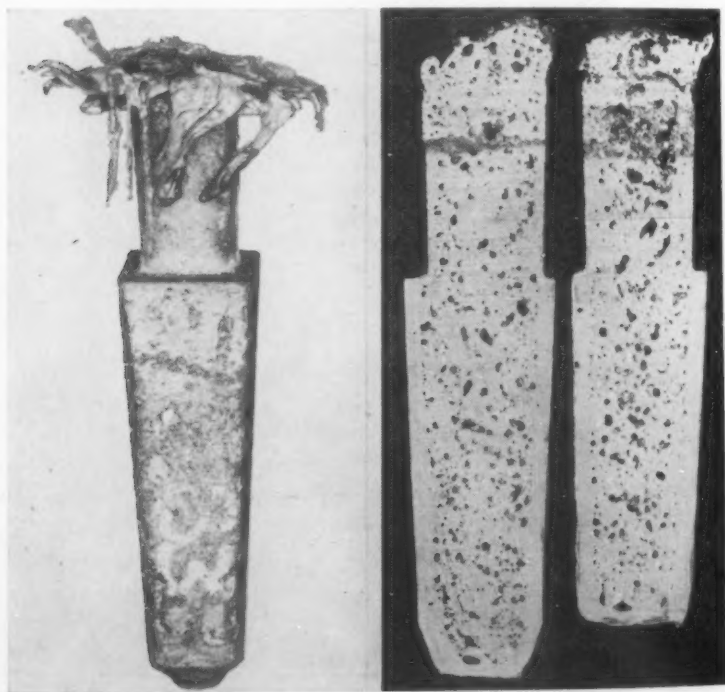
FIG. 1—EXTERNAL (LEFT) AND INTERNAL (RIGHT) EVIDENCES OF THE GAS EFFUSION CAUSING "BLEEDING." SPECIMENS ARE 100-LB. INGOTS OF 5 PER CENT CHROMIUM STEEL KILLED WITH ALUMINUM.
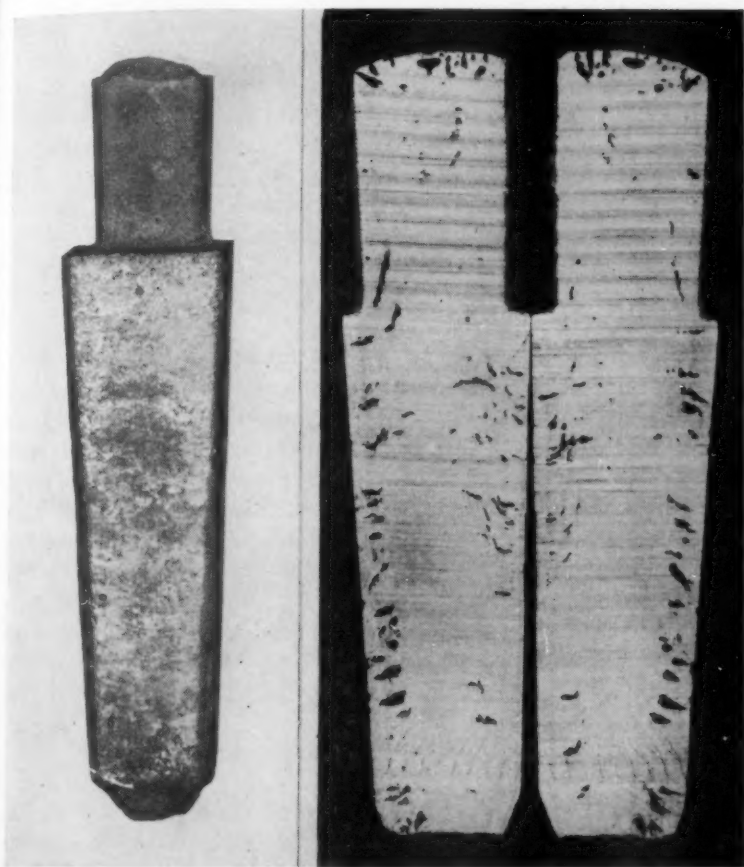
Fig. 2—Exterior (Left) and Interior (Right) of Similar Ingots Showing the More Moderate Type of Effusion Sometimes Referred to as "Rising," or "Backing Up."

"bleeding" and "rising," respectively. It is plain that both resulted from a late effusion of gas, and that they differ principally in the physical violence committed by that effusion.

Carbon-Oxygen Reaction as a Source of Gas

5. By definition, the carbon-oxygen reaction as an important gas producer in the phenomenon under consideration becomes excluded. In the first place, the high percentage of metallic deoxidizers present in most "killed" ferrous metals would seem to prevent carbon from reacting with oxygen that might remain. Secondly, even if the carbon did react with some oxygen, it seems impos-

sible to explain such extensive gaseous effusions upon the amount of carbon oxides that might form. And, thirdly, actual analyses of the gases given off by such metals during solidification do not show important quantities of carbon oxides.

6. With oxygen eliminated as the producer of an abundant gaseous phase, the problem is greatly simplified, since only three common elements in ferrous metals either comprise or create a gaseous phase: oxygen, hydrogen, and nitrogen.

Experiments Demonstrating Gassiness and Bleeding Caused by Hydrogen

7. To demonstrate the effects of the remaining two gases, a group of experiments was performed using a cast iron having such a high silicon content—about 15 per cent—that the chemical activity of oxygen must have been negligible. Ten-pound melts were made from identical charges of steel punchings, graphite, ferromanganese, and 77 per cent ferrosilicon. A magnesia crucible in an induction furnace was used for melting, and the temperature during the treatment of the melt was maintained at approximately 2600°F. The metal was variously cast in chill (iron) and sand molds, the latter comprising a green-sand type and a type dried by heating to a bright red. They will be referred to as chill, green-sand, and dry molds. Castings were ¾-in. in diameter and 8-in. long.

Effect of Hydrogen

8. Sample castings of the three types were poured from the first melt. Through the remaining liquid metal hydrogen was bubbled for 2½, 5, and 10 minutes total time, respectively, sample castings being poured after each treatment. Figure 3 shows the fractured sections of all 12 bars, representing chill, green-sand, and dry-mold castings of this high-silicon cast iron as it is affected by hydrogen absorbed from gaseous H_2. Note that 2½ minutes of bubbling the gas is sufficient to make the metal so "spongy" that additional treatment scarcely shows further effect. The chill specimens, of course, respond but slightly to the gasifying effect because the metal cools too quickly in so small a section. However, the very fact that porosity does not show itself signifies that the gas is retained by the metal, to be available for subsequent anomalies, perhaps, during treatment of the metal. One effect is immediately visible, even on these specimens, in the form of marked ingotism, or columnar structure. Note the difference between the

| | (A) | (B) | (C) |

(1)
(2)
(3)
(4)

(A)
(B)
(C)
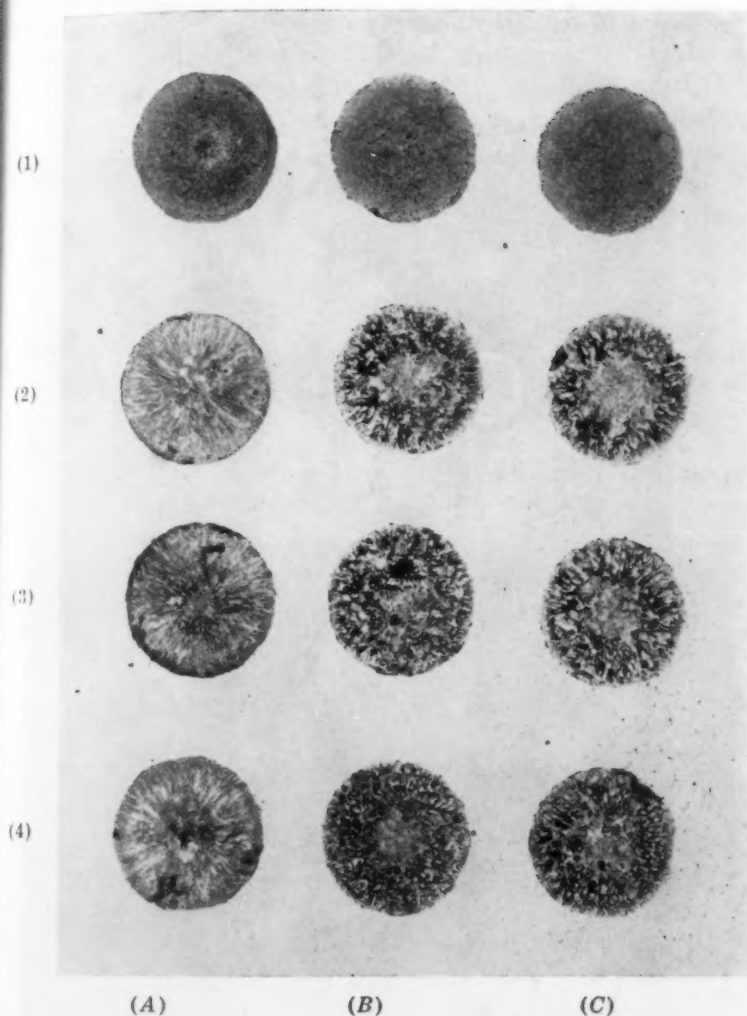
(1) As Melted
(2) H₂ 2-½ Minutes
(3) H₂ 5 Minutes
(4) H₂ 10 Minutes

(A) Chill
(B) Green Sand
(C) Dry Sand

Fig. 3—Transverse Fractures of High-Silicon Cast Iron Treated as Noted, Showing Porosity Caused by Bubbling Tank Hydrogen Through the Liquid Metal Before Casting.

first two chill-cast specimens. This role of hydrogen is discussed elsewhere[1]*. Other effects include abnormal chilling[2, 3] and malleableizing properties[4, 5] for iron, and problems of ductility and cracking for steel[6].

(1)

(2)

(3)

(4)

(A)   (B)   (C)

(1) AS MELTED
(2) N₂ 2-½ MINUTES   (A) CHILL
(3) N₂ 5 MINUTES   (B) GREEN SAND
(4) N₂ 10 MINUTES   (C) DRY SAND
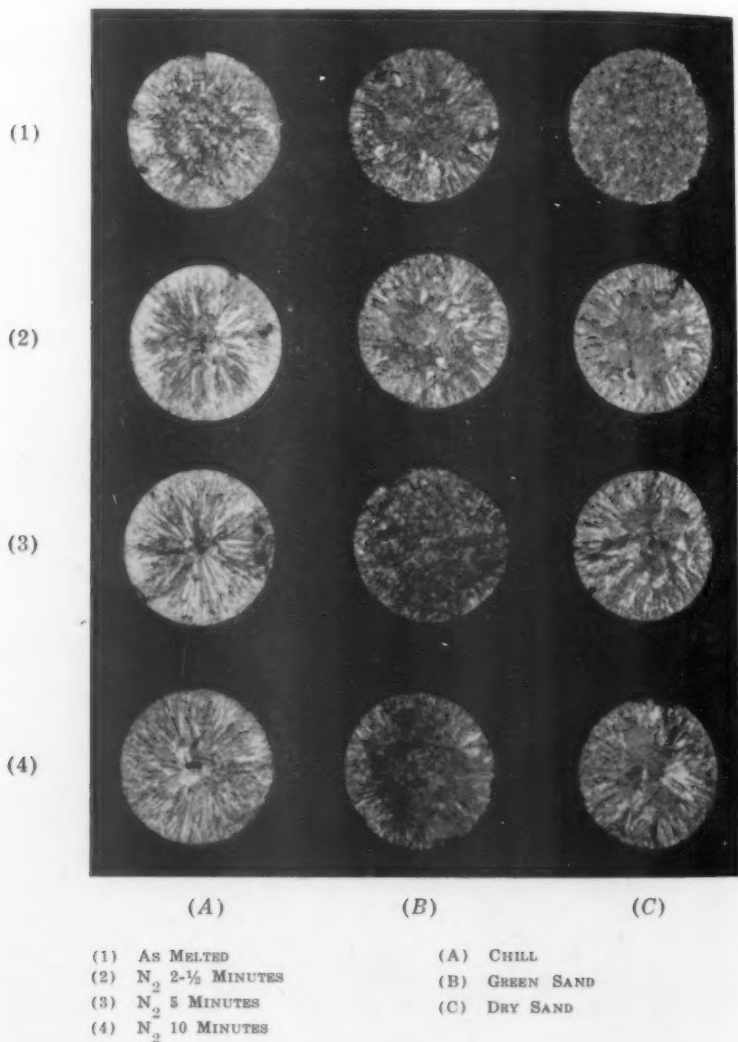
FIG. 4—SPECIMENS SIMILAR TO THOSE IN FIG. 3 EXCEPT THAT THE MELT WAS TREATED WITH NITROGEN INSTEAD OF HYDROGEN.

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* Superior numbers refer to bibliography at the end of this paper.

Effect of Nitrogen

9. In contrast, nitrogen causes no porosity whatever when that gas is bubbled through most ferrous metals. A duplicate test of the one just discussed, but using nitrogen instead of hydrogen, yielded specimens that were completely sound, except for minor porosity in the green-sand castings that will soon be shown to be caused by hydrogen. Their cross-sections are shown in Fig. 4. A porosity that *is* attributable to nitrogen is an exceptional case that may be disregarded while the fundamental nature of porosity is being clarified.

Contrasting Effects of Hydrogen and Nitrogen

10. The contrasting effects of hydrogen and nitrogen can be shown in another way, for the gassiness just discussed may lead to bleeding, which is also under consideration. In Fig. 5 are shown the sides and tops of two dry-sand castings that had been treated for 5 minutes with hydrogen and nitrogen respectively. Nearly half of the casting exuded through its top just before solidifying
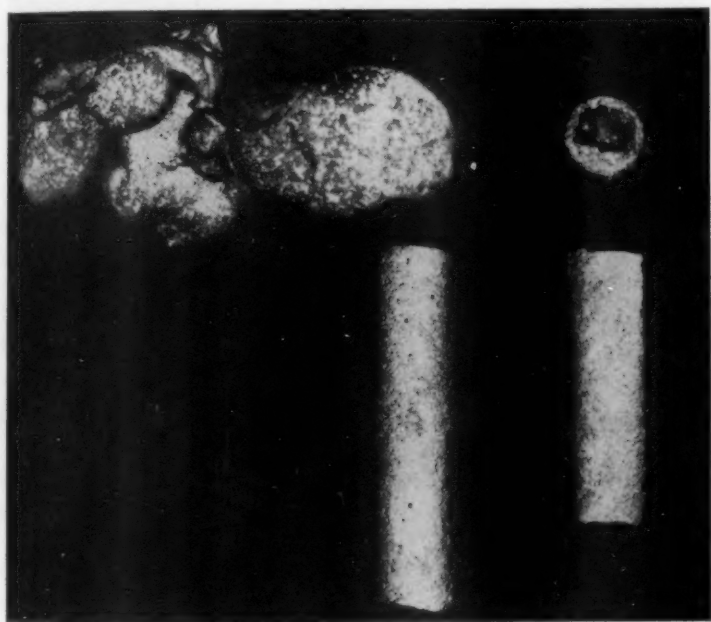
(1)   (2)
H₂ 5 MINUTES   N₂ 5 MINUTES

FIG. 5—SIDES AND TOPS OF TWO DRY-SAND CASTINGS MELTED IDENTICALLY, EXCEPT THAT ONE WAS TREATED FOR FIVE MINUTES WITH HYDROGEN AND THE OTHER WITH NITROGEN.

	(A)		(B)		(C)
(1)	N₂ 10 Minutes	(A)	Chill		
(2)	H₂ 2 Minutes	(B)	Green Sand		
(3)	H₂ 5 Minutes	(C)	Dry Sand		
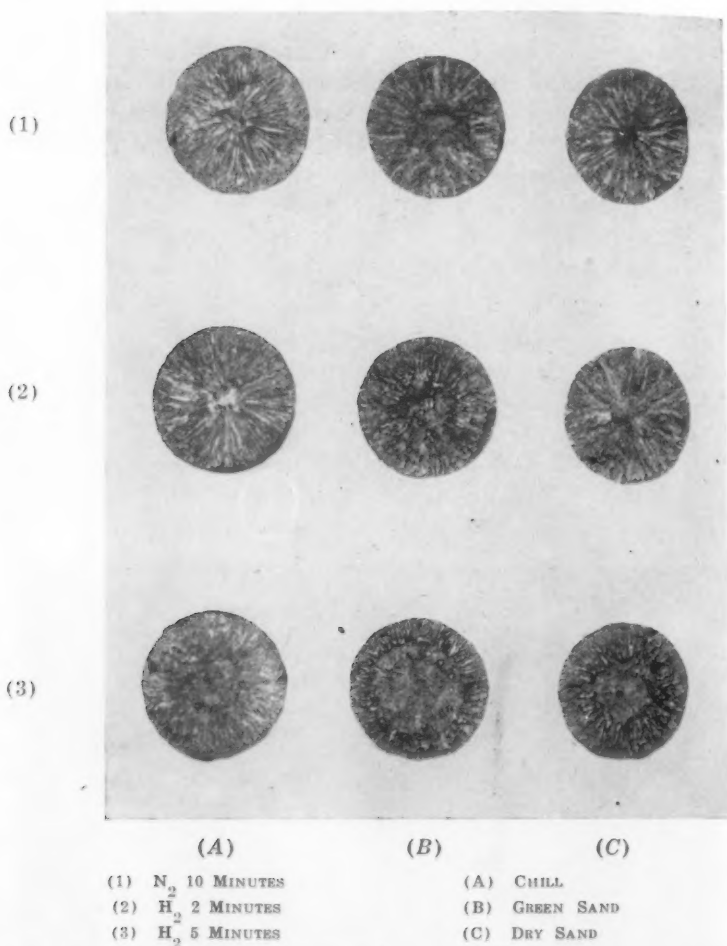
Fig. 6—Transverse Fractures of High-Silicon Cast Iron Treated in the Melt First with Nitrogen and Then with Hydrogen, as Indicated.

when hydrogen had been bubbled through the melt, whereas the nitrogen-treated metal piped deeply. The extruded metal typifies "bleeding."

11. Similarly, hydrogen added *after* a nitrogen treatment will cause its usual porosity. Nitrogen was bubbled through one melt for 10 minutes. Then hydrogen was added in 2- and 5-minute treatments, sample castings being made after each. The transverse fractures are shown in Fig. 6, and the tops of the green- and dry-sand castings are shown in Fig. 7.

DRY SAND

GREEN SAND

N₂ 10 Minutes   H₂ 2 Minutes   H₂ 5 Minutes
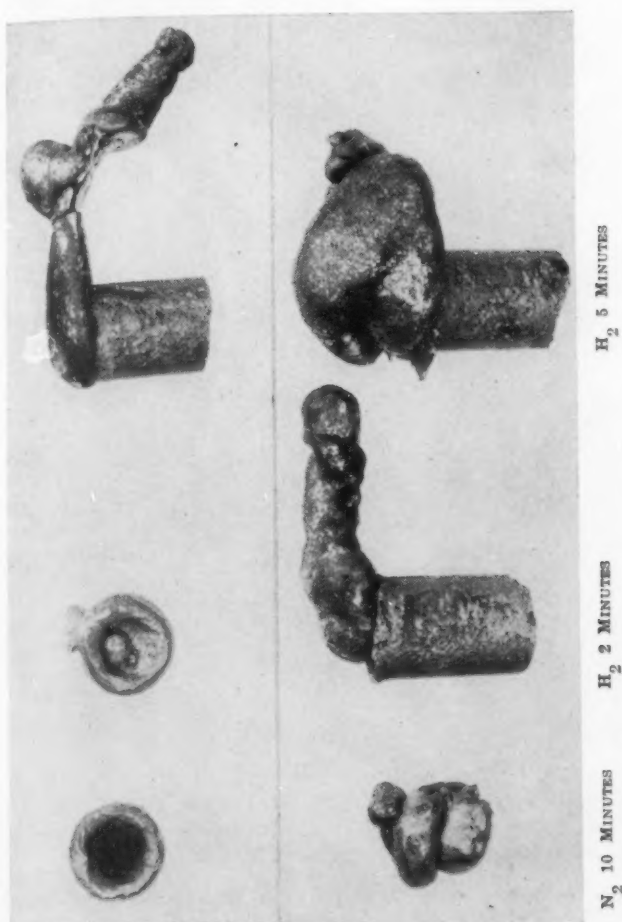
Fig. 7—Tops of Castings from Fig. 6 for Green-Sand and Dry-Sand Molds.

(A)	(B)	(C)
(1) N₂ 10 Minutes		(A) Chill
(2) H₂ 2 Minutes		(B) Green Sand
(3) N₂ 10 Minutes		(C) Dry Sand
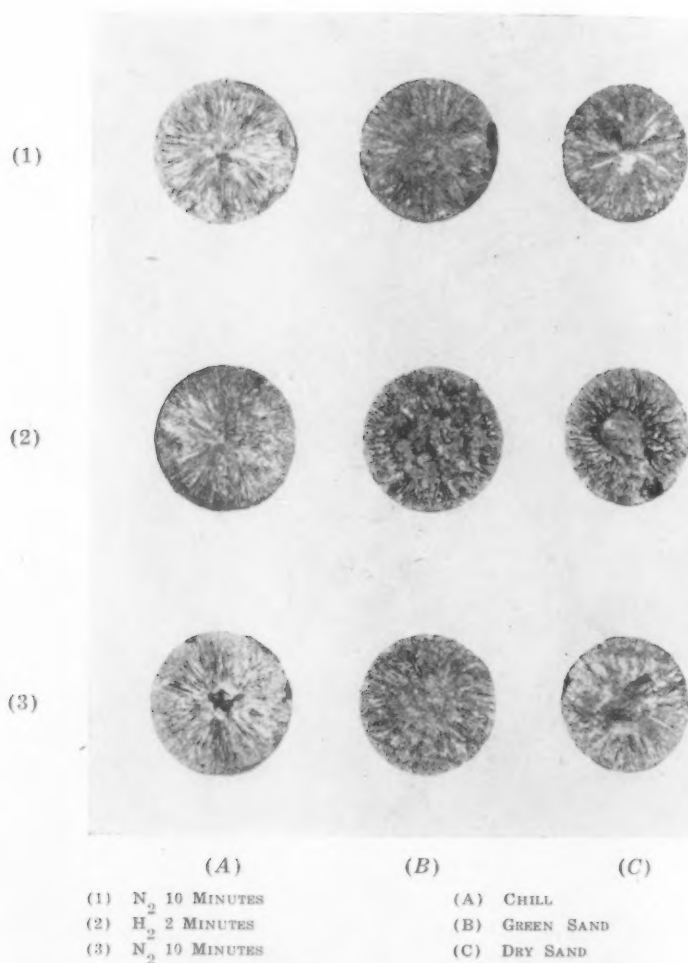
FIG. 8—SIMILAR TRANSVERSE FRACTURES OF METAL AS INDICATED, SHOWING THE EFFECT OF A NITROGEN FLUSH ON GASSINESS CAUSED BY HYDROGEN.

12.   Note in Fig. 6 that the 2-minute hydrogen treatment was sufficient to cause gassiness in the green-sand casting, but not in the dry-sand casting, although the 5-minute treatment caused porosity in both. Also, in Fig. 7, note that even the preliminary 10-minute nitrogen treatment did not prevent bleeding—in the form of a typical "worm"—in the green-sand casting, although the metal cast in dry sand piped deeply.

13.   On the other hand, the reverse treatment of adding the
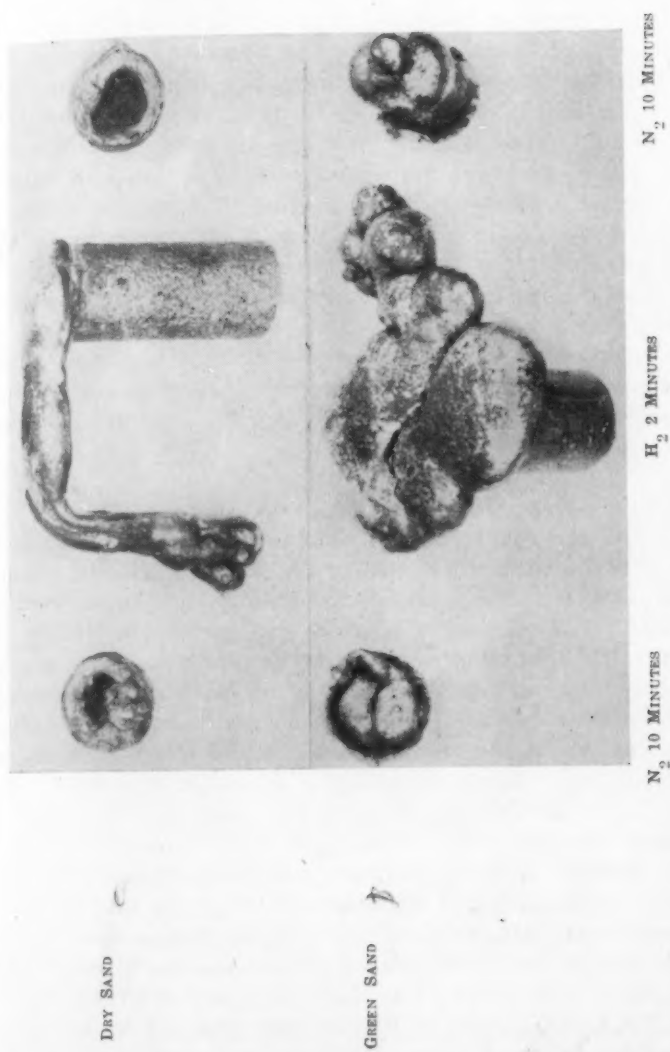
FIG. 9—TOPS OF THE CASTINGS FROM FIG. 8 FOR GREEN-SAND AND DRY-SAND MOLDS.

hydrogen first and the nitrogen afterward leaves the beneficial effect of the nitrogen treatment uppermost. This method of removing hydrogen by flushing the melt with nitrogen, or some other hydrogen-free gas, is time-tested since the classic research by Allen on copper[7] and the later researches of Swinden and Stevenson[8, 9] Good[10], and others on ferrous castings. The British Cast Iron Research Association, for example, adopted carbon dioxide—a gas itself—as a "gas-removing" flush for cast iron[11]. The principle, which will be discussed later, involves more or less a mechanical removal of the dissolved gas by creating spaces within the liquid which are vacua so far as the hydrogen is concerned. As the bubbles pass through the melt, hydrogen evaporates into them and is carried away, just as water evaporates into our atmosphere when the humidity is low.

14.   The object, then, is to expose as large a surface of the liquid metal as possible to an atmosphere having as low a hydrogen content as possible so that hydrogen will evaporate from the metal.

Removal of Hydrogen by Flushing with Nitrogen

15.   In Figs. 8 and 9 the effect of flushing is demonstrated. A melt of the same iron was treated for 10 minutes again with nitrogen; and, as in the previous experiment, hydrogen was next introduced for 2 minutes. Then nitrogen was re-admitted for 10 minutes. The first set of castings was sound, the second set porous, and the third set sound (See Fig. 8). In Fig. 9 the tops of the castings from the green- and dry-sand molds are shown. The dry-sand group makes a perfect representation of the action; whereas the green-sand castings, though showing a similar comparison, once again display the omnipresent proof that green sand is a source of hydrogen for the casting.

16.   Another type of test was also conducted to give some quantitative indication of the beneficial effect of a nitrogen flush. Hydrogen was bubbled through the melt for 10 minutes, in contrast to the previous treatments, and then nitrogen was introduced for 1-, 2-, and 5-minute periods, castings being poured after each addition. The results, shown in Fig. 10, may probably be interpreted as showing that the 1-minute treatment was insufficient, but that 2 minutes of the nitrogen bubbling effectually prohibited the gassiness caused by the hydrogen, another 3 minutes showing no further change. These results, of course, can be interpreted only qualitatively, for they must vary in degree with many factors.

(A)   (B)   (C)

(1) H$_2$ 10 Minutes   (A) Chill
(2) N$_2$ 1 Minutes   (B) Green Sand
(3) N$_2$ 2 Minutes   (C) Dry Sand
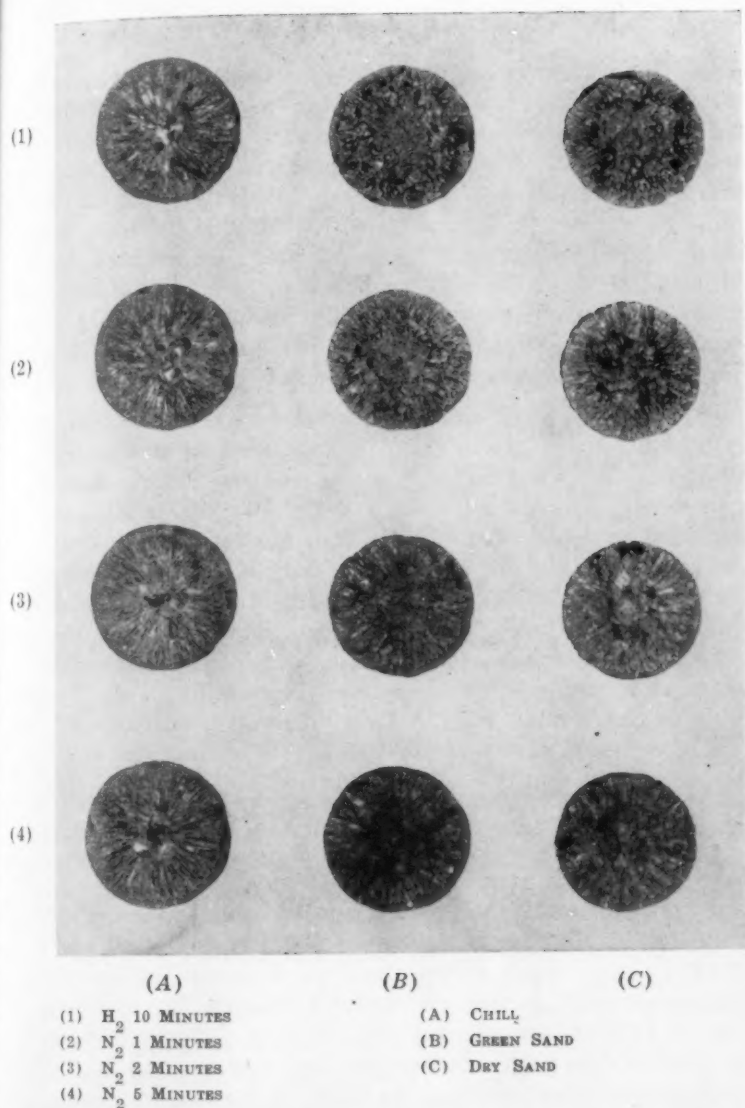(4) N$_2$ 5 Minutes

Fig. 10—Transverse Fractures Showing the Progressive Effect of a Nitrogen Flush
on Removing Gassiness Caused by Hydrogen.

It is interesting, however, in comparing this whole group of figures
to find that a 2-minute treatment of either gas was sufficient to
erase the effects of even a 10-minute treatment with the other gas.

INDIRECT, OR OPERATIONAL, CAUSES OF POROSITY

17.   By indirect causes of porosity we simply mean those opera-
tional factors in the methods of melting and casting that influence
the hydrogen content of the molten metal. Probably too often these
indirect factors are blamed with consequent neglect of the actual
cause. For example, if porosity is traced to damp molds, the found-
ry may overcome the trouble by caring for that particular opera-
tional factor; nevertheless, the damp mold does not "cause" the
porosity. Hydrogen causes the porosity; and, because there are
other sources of hydrogen than damp molds, the foundry which
regards just the operational factor may well run into the same
problem from some other source of the gas.

18.   The proper viewpoint would seem to be to recognize that
hydrogen is the "gremlin" and then to work toward the end of
mitigating all the numerous sources of that gas. Some authors have
gone so far as to speak of "mold gases" and "gases injected into
the metal from the mold"; but that is not far enough. Only one
gas is injected, and we know what that gas is. Besides hydrogen,
the only other element in steam, which in turn is probably the
only gas that is not a hydrogen-carbon compound in all possible
mold gases, is oxygen. Oxygen is not only half as abundant as
hydrogen in steam, but it forms solid phases with the metal which
have no measurable gas pressure whatever. Furthermore, the
rate of diffusion of oxygen through iron is of the order of a frac-
tion of one per cent of the rate of diffusion of hydrogen. The in-
jection of oxygen may be forgotten.

Moisture in Molds as a Source of Hydrogen

19.   Consequently, the problem is to uncover the various sources
of hydrogen. Moisture in the mold is certainly an outstanding one.
In the figures already shown, the green-sand castings consistently
showed worse effects than the dry-sand castings. The specimens in
Fig. 11, furthermore, indicate that the most elaborate precautions
for removing the hydrogen from the melt may be of no avail when
there is sufficient moisture in the mold. It must be emphasized, of
course, that such conclusions apply only to the present tests, al-
though the moisture content of the green sand was normal. The
ratio of surface to volume for the casting, the avidity of the differ-
ent ferrous metals for moisture, and such factors, will change the
ability of the gas to enter the metal and to concentrate within it
in sufficient quantities to do harm.

DRY SAND

GREEN SAND

CHILL

N_2
5 MINUTES

N_2
10 MINUTES
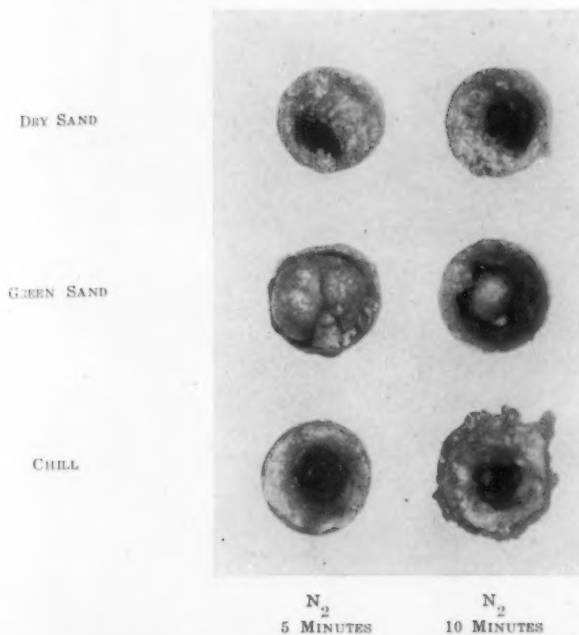
FIG. 11—TOPS OF CASTINGS OF METAL GIVEN AN EXTENDED NITROGEN FLUSH IN AN AT-
TEMPT TO ELIMINATE BLEEDING IN GREEN-SAND CASTINGS.

20. For the specimens in Fig. 11, a 5-minute flush with nitro-gen prevented bleeding except in the green-sand casting. An addi-tional 5 minutes did show further improvement, but bleeding still occurred. This test has another interesting feature, too, for it will be recalled that a 2-minute flush stopped porosity in the dry-sand castings (Fig. 10). Nevertheless, the green-sand specimens in Fig. 11 indicate that hydrogen is still being removed from the *melt* after 5 and 10 minutes of flushing. The melt, then, in any condi-tion must always be considered as having some *threshold hydro-gen content* that may or may not be able in itself to cause trouble. If not, it will still give the metal a certain degree of sensitivity to further hydrogen pickup, such that a very small absorption in some cases might raise that threshold value above the *critical*, or *minimum*, value at which deleterious activities of the gas begin.

21. As a prolific source of hydrogen, moisture can easily be condemned. Figures 12 and 13 show the transverse fractures and the tops of castings made from metal through which *steam* was bubbled for various periods before casting. Comparison of these with the specimens in Figs. 6 and 7 shows that steam, if its effect

(1)

(2)

(3)

(A)   (B)   (C)

(1) AS MELTED
(2) H_2O 2 MINUTES
(3) H_2O 5 MINUTES

(A) CHILL
(B) GREEN SAND
(C) DRY SAND
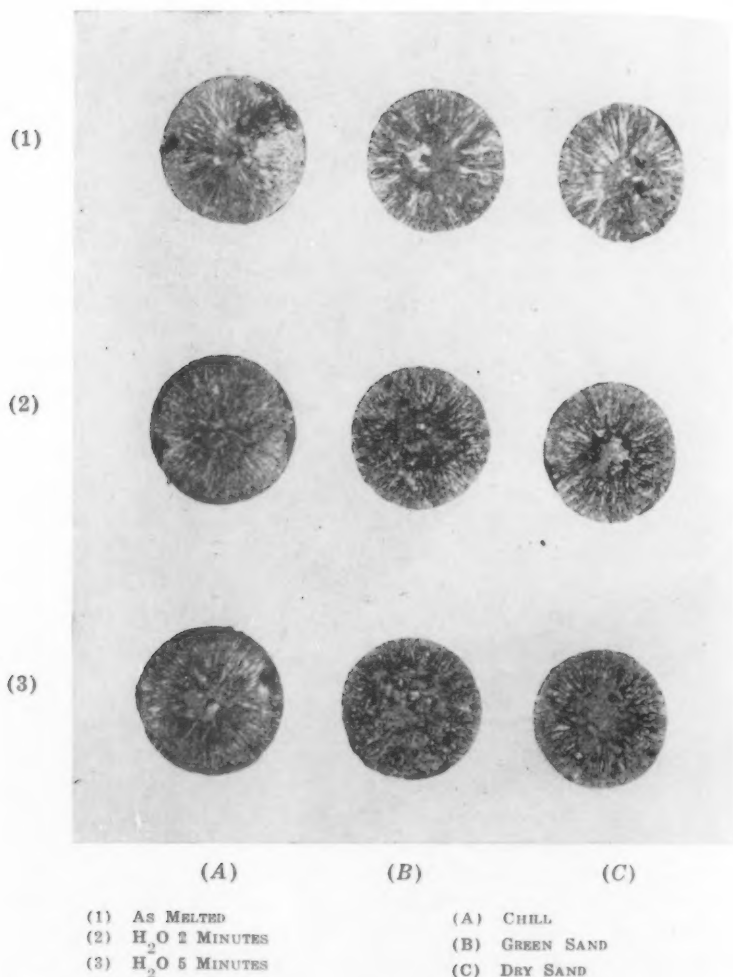
FIG. 12—TRANSVERSE FRACTURES ILLUSTRATING THE EFFECT UPON POROSITY OF BUBBLING STEAM THROUGH THE LIQUID METAL BEFORE CASTING.

differs at all from that of pure gaseous hydrogen, is an even more virulent form of inoculator. Also, there seems to be but little difference between the 2- and the 5-minute treatment. The action of moisture on hot ferrous metal is without doubt both extremely rapid and effective.

22. As a source of moisture, and hence of hydrogen, green sand and mold washes have been long recognized. There is also
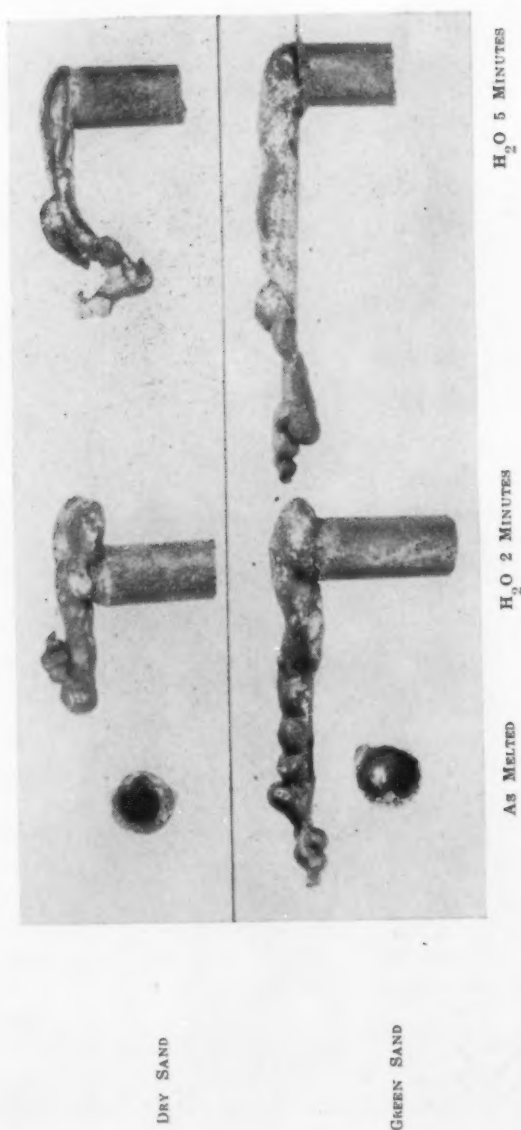
Fig. 18—Tops of the Castings from Fig. 12 for Green-Sand and Dry-Sand Molds.

general agreement that rusty scrap, slaked lime, damp charge, high humidity, and such factors, all of which are obvious sources of moisture, may often be related to hydrogen-caused defects such as gassiness and low ductility.
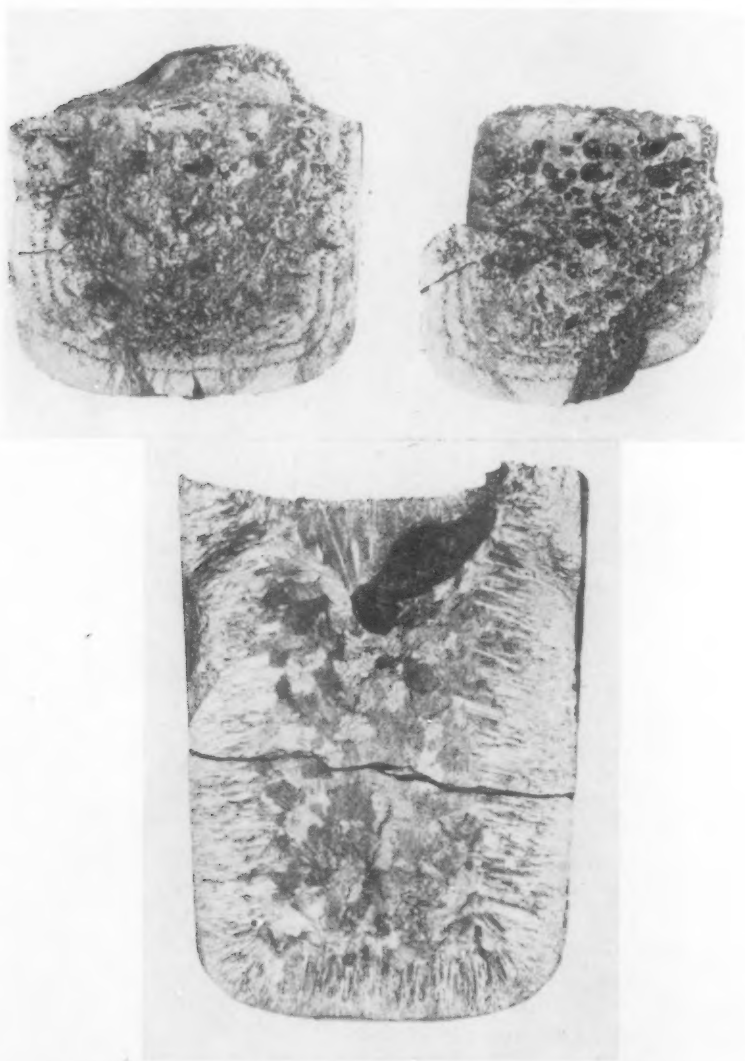
FIG. 14—FRACTURES OF LARGER CHILL-CAST SPECIMENS ILLUSTRATING HYDROGEN PICK-UP FROM NEW FURNACE LININGS. TOP—WASH HEAT, SHOWING SEVERE POROSITY. BOTTOM—NITROGEN FLUSHED, SHOWING NORMAL SOUNDNESS WITH PIPING.
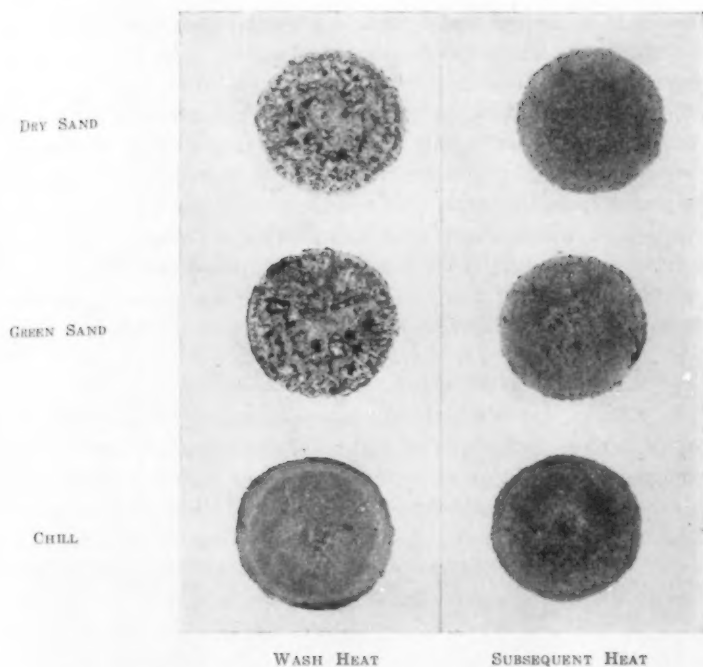
FIG. 15—TRANSVERSE FRACTURES OF SAND-CAST SPECIMENS ILLUSTRATING HYDROGEN PICK-UP FROM NEW FURNACE LININGS.

Moisture in Furnace Linings or Crucibles as a Source of Hydrogen

23. Less recognized, perhaps, is the furnace or crucible lining itself. In the present tests, a wash heat was always poured first to reduce inherent effects from the crucible as much as possible. These effects appear to be of two general types: (a) In new linings, hydrogen is probably present as moisture, or hydrate, which is driven off only with difficulty and, therefore, responds much as does moisture in the mold in regard to injecting the metal with hydrogen; (b) In used linings, hydrogen may also be present, probably as an expression of the equilibrium sought by the previous melt.

24. The interesting, and marked, case of hydrogen pickup from a crucible previously used for a hydrogenized melt is described in another paper[1] and will not be repeated here. The other case, wherein hydrogen is absorbed from a fresh lining, is illustrated in Fig. 14. The wash heat was cast in a chill mold which was sufficiently large so that gassiness had time to develop. With it, for com-

parison, is a similar casting made after the nitrogen flush in the tests shown in the previous Fig. 8, proving that the normal, hydrogen-free condition is sound.

25. In case it might be argued that the charge, rather than the furnace lining, was responsible for the injection of hydrogen, a group of customary transverse fractures is presented in Fig. 15. The metal from the wash heat shows porosity as bad as any of the metal deliberately treated with hydrogen or steam; whereas the next castings charged with the same material show ordinary soundness. Perhaps these tests throw some light on known instances of inferior results obtained with the first metal cast from new linings.

Other Sources of Hydrogen

26. Other less obvious sources for hydrogen are also often present, and in many cases may be the unrecognized factors behind foundry troubles. For example, all bonded foundry sands contain hydrogen in some form which may be released to the metal by oxidizing or by carburizing effects, as well as by straight absorption. Tapping spouts, ladle linings, nozzle and stopper assemblies, or cups and gates—all should be under suspicion in regard to hydrogen pickup. Preheating these items is a move in the right direction, but is assuredly incomplete, if for no other reason than that the sand remains bonded, since the bond is hydrogeniferous. In addition, the customary preheating with a flame or atmosphere containing hydrogen must keep some of that gas within the refractory, according to the evidence just mentioned for hydrogen being inherited from heat to heat[1]. "Sooting" a mold with an acetylene flame, incidentally, deposits a substance having a large ability to adsorb hydrogen.

27. That some of these factors are worthy of consideration is indicated by the following experiment. First, a 5 per cent chromium steel was melted in an induction furnace from a charge of Armco iron, ferrochromium, and the customary minor additions of ferromanganese, ferrosilicon, and carbon for a 4 - 6 per cent chromium steel. Two 60-pound ingots were chill-cast shortly after the melt was killed with aluminum. One mold was plugged at the bottom with a baked sand core; the other was plugged with carefully preheated K-30 silica brick. In all other respects the molds were identical, the metal was from the same heat, and the pouring factors were essentially constant. Yet, cross-sections of the two ingots (See Fig. 16) show quite a marked difference in the porosity of the two, the ingot from the mold with the dried refractory plug

having considerably less gassiness. This steel, of course, was sensitive to gassiness, and the results must not be construed to mean that a sand core will always cause porosity.

28. The specimen in Fig. 17, however, suggests the same effect. A 3 per cent nickel steel of the SAE 3430 type was melted in an induction furnace. Before casting, nitrogen, dried by passing through a train containing both "Drierite" and concentrated H_2SO_4, was bubbled for three minutes through the liquid steel. Cast in a chill mold with a bonded sand hot-top, the ingot showed no porosity except within the neck enclosed by the hot-top; and the porosity there was of the pinhole type attributable to hydrogen injected from the solidifying wall of the ingot[12].

Ammonia as a Source of Hydrogen

29. In concluding this discussion of the indirect factors causing hydrogen troubles, one more set of tests is worth considering. A melt of the high-silicon cast iron, as used before, was treated
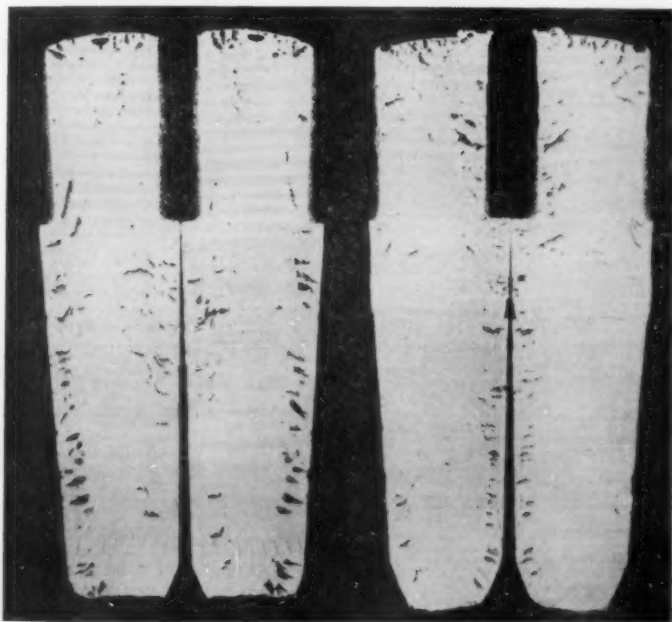
Fig. 16—Cross-Sections of Two 60-Lb. Ingots of 5 Per Cent Chromium Steel Cast in Iron Molds Under Identical Conditions Except That the Bottom Mold Plugs Were of Different Materials. Left—Bottom Plug of Ordinary Bonded Sand. Right—Bottom Plug of Carefully Dried Silica Brick.

FIG. 17—CROSS-SECTION OF 3 PER CENT NICKEL STEEL OF THE SAE 3430 TYPE DEHY-
DROGENIZED BY A 3-MINUTE NITROGEN FLUSH BEFORE CASTING. NOTE PINHOLE POROSITY
IN NECK, WHICH WAS IN CONTACT WITH THE BONDED SAND HOT-TOP.

while molten with ammonia gas. Figures 18 and 19 show that am-
monia must be classed as a typical hydrogenizer, for there is truly
little to choose between its effects and those shown in previous
figures for hydrogen and water. One need not search the litera-
ture far to find instances of investigators using ammonia gas in
contact with steel without appreciation for its hydrogenizing
ability. Indeed, often nitrogen has been wrongly blamed for caus-
ing the defects in steel that resulted from so-called "nitriding"
processes using ammonia. That oversight is especially remarkable
in view of the fact that ammonia contains three times as much
hydrogen as it does nitrogen, that the nitrogen forms a solid phase
with iron and many of its constituents in contrast to the truly
gaseous hydrogen, and that the absorption of nitrogen is a much
slower process than the absorption of hydrogen.

(A)   (B)   (C)

(1) Untreated   (A) Chill
(2) NH_3 2 Minutes   (B) Green Sand
(3) NH_3 5 Minutes   (C) Dry Sand
(4) NH_3 10 Minutes

Fig. 18—Transverse Fractures Illustrating the Effect Upon Porosity of Bubbling
Gaseous Ammonia Through the Liquid Metal Before Casting.

DRY SAND

GREEN SAND

UNTREATED   NH$_3$ 2 MINUTES   NH$_3$ 5 MINUTES   NH$_3$ 10 MINUTES

FIG. 19—TOPS OF THE CASTINGS FROM FIG. 18 FOR GREEN-SAND AND DRY-SAND MOLDS.

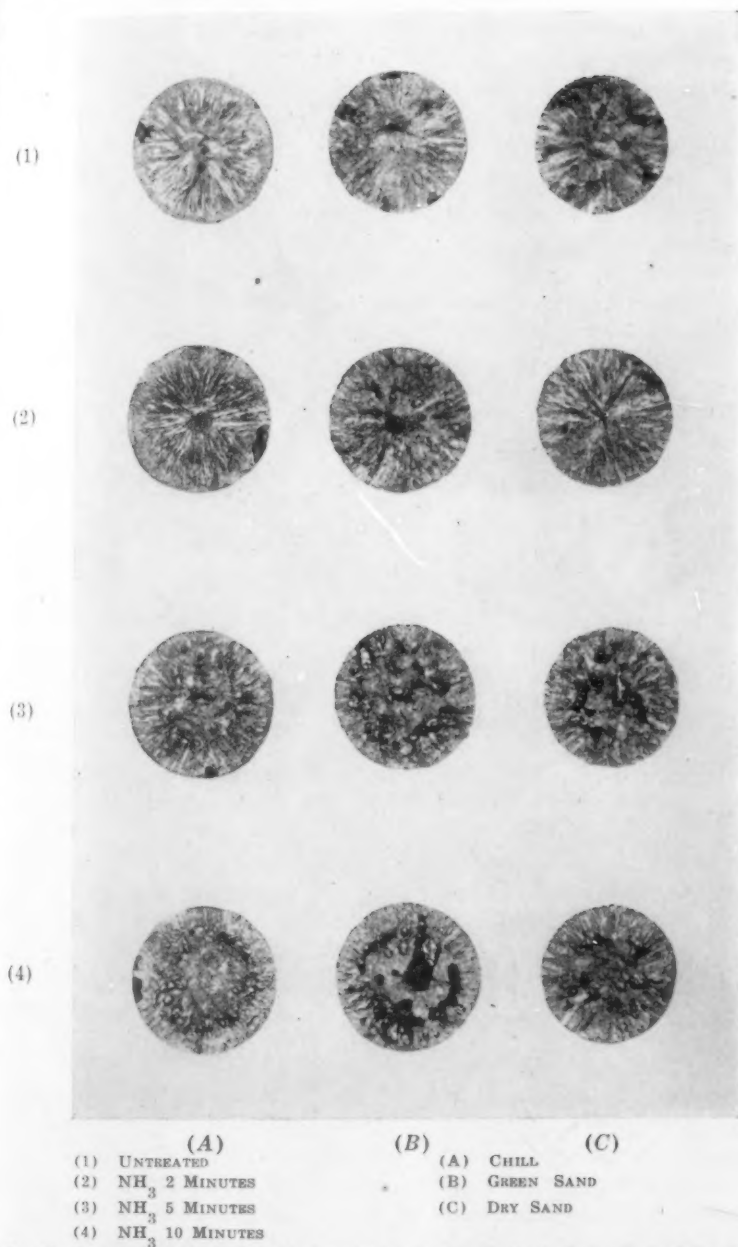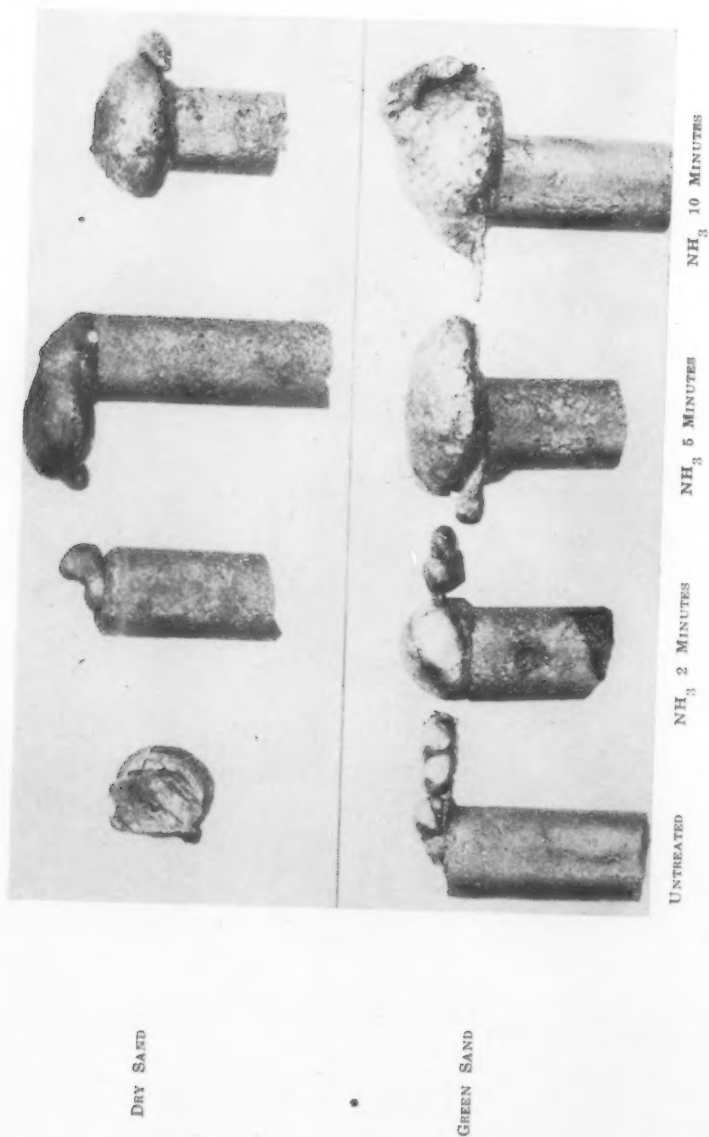*Relation of Operational Factors to Gassiness*

30. It is apparent, therefore, that such operational factors as high pouring temperatures, closed furnace, lack of a boil, moist furnace additions, and improperly dried refractories have a relation to the troubles of gassiness and low ductility.

31. On the other hand, a boil is a well known preventative of hydrogen troubles. In a basic furnace, a lime boil may be used, but the regular carbon boil is probably most effective. In lieu of a carbon boil, however, a simulated boil produced by bubbling any hydrogen-free gas, such as carbon dioxide or nitrogen, through the bath will be effective. Iron oxide added to produce a carbon boil may be doubly effective by removing hydrogen according to the reaction

$$FeO + 2H = H_2O + Fe.$$

DIRECT, OR FUNDAMENTAL, CAUSES OF POROSITY

Precipitation Pressure and the Solvent Power of Steel for Hydrogen

32. Of all the obvious factors affecting bleeding, other than gas content, none is more outstanding than the composition of the steel; and, from among the fluctuating mass of opinion, the best agreement is on the effects of nickel and chromium. Irons and steels containing chromium, particularly the 5 and 12 per cent chromium steels, are notoriously liable to gassiness and bleeding. Steels containing 17 per cent chromium seem to be less so; and nickel in large amounts is appraised by some observers as generally exerting a strong influence toward soundness. Why?

33. Plainly, a fundamental factor of hydrogen-caused gassiness lies in the difference between liquid and solid steel as solvents for hydrogen. A second factor having comparable importance regards the actual amount of hydrogen absorbed by the liquid metal. By assuming one of these to be constant, the nature of the other can be studied.

Solubility of Hydrogen in Solid and Molten Iron

34. Because the items influencing the absorption of hydrogen by liquid iron and steel have already received some discussion, an isolated study will now be made of the first factor—or the change in the metal itself as a solvent for the gas through the temperature range of solidification. Let it be clearly understood that the following remarks refer only to this factor of changing solvency,

and that equal amounts of hydrogen are assumed for each liquid
—an unlikely condition in practice. That is, we have seen that
factors increasing the hydrogen absorption, such as damp molds,
in themselves cause unsoundness; but now we shall investigate
why, with absorption factors essentially constant, some types of
metal may be more liable to gassiness than others.

35. A glance at the Fe-H curve in Fig. 20 suffices to show
that under the conditions there postulated, the greater portion of
that gas held by the liquid metal tends to be lost during solidifica-
tion. Roughly, the loss of three volumes of hydrogen, measured
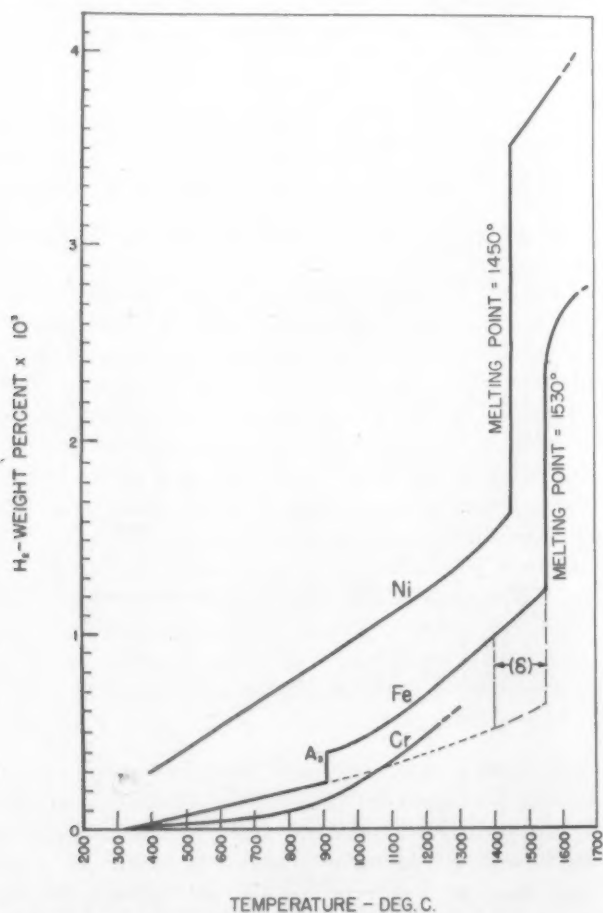

FIG. 20—CURVES SHOWING THE ABSORPTION OF HYDROGEN BY IRON, NICKEL, AND CHROMIUM
FROM HYDROGEN AT A PRESSURE OF ONE ATMOSPHERE.

at the temperature of the melt, per volume of metal is not unlikely even for normal castings. Because most of the gas must get out by way of the core of the casting, little wonder it is that some metal is occasionally extruded.

Effects of Nickel and Chromium on the Solubility of Hydrogen in Iron

36. In the same figure, a curve for Ni-H shows that, for a given temperature and pressure, nickel is a better solvent for hydrogen than is iron. Alloys of iron and nickel, in general, absorb amounts of hydrogen roughly proportional to the composition of the alloy[14, 15]. The Cr-H curve, on the other hand, indicates that chromium is a poorer solvent for hydrogen than is iron. As a matter of fact, although the data of Luckemeyer-Hasse and Schenck[14] and Martin[16] agree in designating that curve for Cr-H, Russian research shows that hydrogen is completely insoluble in chromium, and that addition of chromium to steel markedly lowers the solubility for that gas.

37. Qualitatively at least, the picture is entirely coherent, for pure iron shows an especial preference for hydrogen when the structure is face-centered cubic (gamma), rather than body-centered cubic (alpha). Nickel has a face-centered lattice, whereas chromium is body-centered. It is generally true that hydrogen dissolves much more readily in metals having a face-centered cubic structure.

38. Gassiness, therefore, whose degree is a rough measure of the change in solubility from liquid steel to solid, should tend to be suppressed by alloying with nickel because the solid nickel steel holds so much more of the gas in solution; whereas chromium should exert the opposite effect because of a decreased solubility.

Solubility of Hydrogen in Gamma and Delta Iron

39. Furthermore, the variation in the shape of the conventional Fe-H curve, as indicated in the figure, has been fairly well established by Schenck[18] and is consistent with the reasonable expectation that the return of iron, during heating, to the body-centered cubic form should similarly bring a reversion of the absorption curve to conform with its trend in alpha.

40. If this is so, an iron freezing into delta will tend to evolve more hydrogen than one freezing into gamma, given the same concentration in the melts. Nickel and chromium not only have high and low solubilities, respectively, for hydrogen, but, as alloying

elements, each favors the corresponding form of iron. Nickel opens, and chromium closes, the gamma loop. Conversely, nickel *narrows* the temperature range of delta iron; chromium *broadens* it.

41. On cooling a steel high in nickel, then, the evolution of hydrogen, which requires a finite period of time, will tend to follow curve I in Fig. 21, bridging the delta dip, if it still exists, because that narrowed temperature range could be traversed too quickly to permit the gases to gather and evolve. On the other hand, a steel high in chromium, having an enlarged delta temperature range, would tend to expel gas along curve II to the bottom of the recess. Furthermore, the chromium undoubtedly deepens that recess below that for pure iron to aggravate evolution even
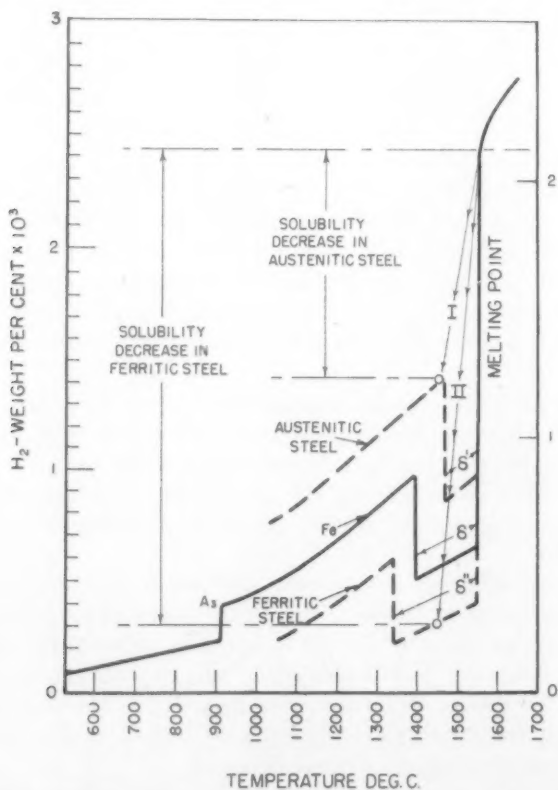
FIG. 21—SCHEMATIC REPRESENTATION OF THE ROLE OF THE DELTA RANGE IN FERROUS ALLOYS IN ITS EFFECT UPON THE EVOLUTION OF HYDROGEN DURING SOLIDIFICATION. CURVES I AND II DEPICT A GENERALIZED PATH OF EVOLUTION FOR AUSTENITIC AND FERRITIC ALLOYS, RESPECTIVELY. (VARIATIONS IN MELTING POINTS AND HYDROGEN CONTENTS OF THE LIQUID ARE NOT CONSIDERED.)

more; just as nickel increases the solubility of gamma to minimize the evolution even beyond the effect of the bridging.

42. As a generalization, then, we may conclude that ferritic steels tend more toward gassiness than do austenitic steels*.

43. In practical steel-making, the exceptions to that rule may be numerous because of obscuring factors, principally those changing the hydrogen content at the time of pouring. As just stated, this conclusion refers only to conditions of equal initial hydrogen content, and disregards temporarily the fact that nickel and chromium likewise change the absorbing power of the liquid. Indeed, that factor may be responsible for the general observation that the steels having chromium contents of 17 per cent or better seem less liable to gassiness. That is, the hydrogen content of the steel at the time of pouring must be considerably affected, probably decreased, by the addition of 17 per cent of chromium, so that the curves in Fig. 21, which are based upon pure iron as a matrix, need no longer apply.

FIG. 22—ABSORPTION CURVES FOR HYDROGEN IN MANGANESE AND IN IRON FROM HYDROGEN AT A PRESSURE OF ONE ATMOSPHERE.

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* It is not to be expected that the introduction of a few tenths of a per cent of nickel or chromium, as in the N.E. steels, or even of a per cent or so, in some S.A.E. steels, will have very much effect.

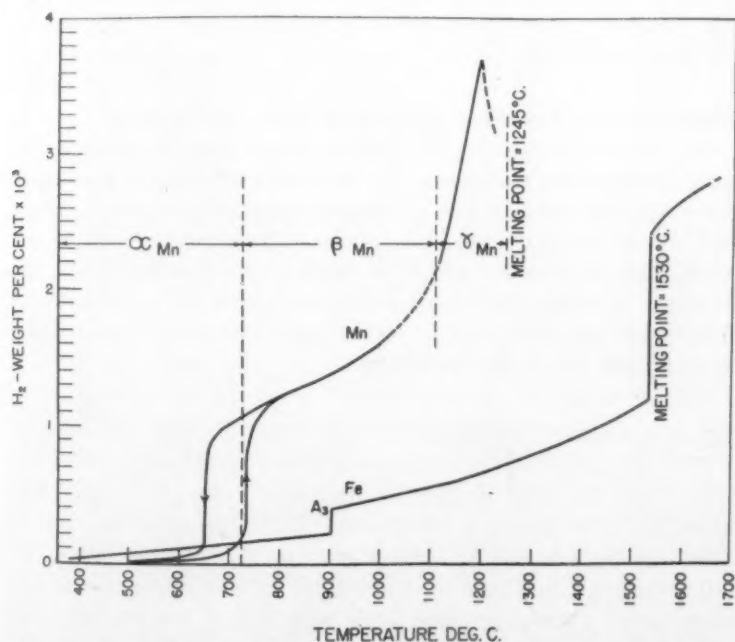44. On the other hand, the beneficial effect of nickel may be obscured if electrolytic nickel is added to the charge, or some other strongly hydrogenizing factor is introduced.

Effect of Manganese on the Solubility of Hydrogen in Steel

45. Manganese certainly alters these phenomena, and its effect can be understood from several viewpoints. First, researches such as that of Herasymenko and Dombrowski[19] agree in showing that the contents of hydrogen and manganese in the steel bath fluctuate together, and closely. Secondly, solubility measurements of hydrogen in manganese by Luckemeyer-Hasse and Schenck[14] (See Fig. 22) show values severalfold higher than for iron; consequently, manganese should be expected to increase the solubility of steel for that gas. Lastly, hydrogen, though seldom mentioned seriously as a deoxidizer, is legitimately one, and a powerful one; whereupon hydrogen, if available, should increase with increasing manganese much as silicon tends to do when a supply is available. The fact that hydrogen is probably always available in some form or other has already been mentioned.

Effect of Silicon on the Solubility of Hydrogen in Steel

46. In the literature, the opinion has often been expressed that silicon, and other metals notable as deoxidizers, markedly increase the solubility of steel for hydrogen, possibly forming hydrides; and several investigators have unsuccessfully sought supernormal solubilities in these metals. The matter now becomes clear when hydrogen receives its due respect as a deoxidizer. An increase in silicon, for example, simply shifts the hydrogen-oxygen ratio toward higher values for hydrogen.

Effect of Other Elements on the Solubility of Hydrogen in Steel

47. In regard to other elements which occasionally exert some influence, the same general explanation probably holds. Some added as ferro-alloys, or as electrolytic metal, may simply bring their own high hydrogen content into the steel. For example, 50 to 100 volumes of hydrogen in one volume of electrolytic metal is not uncommon; whereupon steel made with 1 or 2 per cent of such a metal could obtain a volume of hydrogen equal to the volume of the steel—an amount more than sufficient to cause all of the troubles under consideration. Again, an alloying addition may simply prevent a proper boil from taking place.

Actual Precipitation—The Necessity for Nucleation

48. In the foregoing discussion, factors influencing the dissolving power of iron and its *tendency* to absorb and to desorb hydrogen represent only half the picture, since *hydrogen in itself cannot cause a bubble to form inside liquid iron.* The other half of the picture has remained more or less obscured by a customary loose usage of the word "solubility," when speaking of systems involving a gaseous component, for the *absorption* that occurs under certain conditions of external gas pressure. It is sufficiently important to warrant some digression here.

Effect of Pressure on the Solubility of Hydrogen in Metal

49. Metallurgists find it easy to forget that the common binary diagram is actually only one section taken at $P = 1$ atmosphere through a true solid model whose third dimension is pressure[20]. Because the equilibrium of most metallic systems is so negligibly sensitive to pressure changes, the curves on the section taken at atmospheric pressure are essentially the same as the curves for any other pressure, and but little reason remains for using the true solid model.

50. With a gas-metal system, on the contrary, the third dimension of pressure is indispensable. Without pressure specified, curves such as those in Figs. 20-22 have no meaning whatever, since a curve having any conceivable shape could be as easily obtained by suitable regulation of the pressure variable.

51. Consequently, the curves in those figures express only the *absorption* that occurs when the metal is exposed to molecular hydrogen under one atmosphere constant pressure. The word "solubility" should not be used, unless one is careful to connotate it directly with the pressure. The true solubility, which is a limiting solubility corresponding to the solubility we speak of for carbon in iron, has never been determined for hydrogen. Below that undiscovered limit lie all present-day observations; and the *absorption*, which is often wrongly called solubility, varies with pressure by a square-root relationship[6, 12], an infinite family of curves lying to either side of the ones shown in Figs. 20-22.

Evaporation vs. Precipitation of Hydrogen From Molten Metal

52. As a consequence, these curves cannot be construed to mean that the gas will precipitate when their boundaries are surpassed, since no limiting solubility has been exceeded. A sectional curve from a higher pressure would quickly show that the solubility limit

has *not* been exceeded. They only mean that, as the injecting atmospheric pressure varies, or as the dissolving power of the metal varies, hydrogen will *tend* to *diffuse* into the metal or to *evaporate* from it into any adjoining atmosphere which is deficient with respect to the solubility-pressure relationship. "Evaporate" seems a much better word than "precipitate" for this action, since it expresses a movement toward equilibrium only across some boundary where the equilibrium has been upset by some other agent. "Precipitation" implies an ability to create its boundary, which hydrogen cannot do in the liquid state.

53. A clay ball, for example, brought to a high moisture content in moist air does not "bloat" from internal precipitation of water vapor when air having a lesser humidity is subsequently passed over it. Instead, the moisture evaporates into that atmosphere from exposed surfaces.

54. To clarify this statement further, another simple analogy may be helpful. The solubility of carbon in liquid iron is of the order of 5 per cent at 2500°F. The solubility of hydrogen in liquid iron is not known, but the value lies many times above the curve for one atmosphere pressure. Now, from 3000°F. down to 2000°F. the solubility of carbon in liquid iron, which is a true solubility because the value is essentially constant for any pressure, decreases from 5.5 to 4.5 per cent, roughly speaking. The solubility curve slants from upper right to lower left just as does the hydrogen curve; nevertheless, an iron containing only 2.0 per cent carbon— or any value less than the limiting value of 4.5 per cent—certainly will not precipitate any of its carbon while all of the metal is still liquid. Nor will an iron-hydrogen solution precipitate its hydrogen, because the limiting solubility, or true solubility, lies at some unmeasured position far beyond the absorption ever encountered in practical steel-making.

55. Probably a better analogy, since carbon has too low a vapor pressure to be regarded conveniently as an analogy of a gas, is the Fe-O system. For natural, or planetary, reasons, the limiting solubility of oxygen in iron is always plainly evident, because the oxygen pressure of the atmosphere is far above the oxygen pressure of saturated iron.

56. By surrounding iron with a special atmosphere, however, such as a mixture of hydrogen and moisture, or carbon monoxide and carbon dioxide, the oxygen pressure can be brought below that of saturated iron, whereupon oxidation does not occur. Indeed, that is the fundamental basis of the blast furnace, as it is of most

iron- and steel-making. Then the iron absorbs and desorbs oxygen much as it does hydrogen under ordinary conditions. The oxygen behaves as a gas, just as does hydrogen, and for any constant atmospheric pressure P_0, an "absorption curve" may be established that is a direct analogy of the curves in Figs. 20-22. Only when P_0 exceeds some limiting value, will the true solubility limit be found, free iron oxide will form and will *precipitate*, and increasing P_0 can cause no further absorption of oxygen by the iron. That limit has never been found for the hydrogen-iron system.

57. Conversely, if P_0 is only half, say, of this limiting value, causing an absorption of approximately half the saturation value, decreasing P_0, or removing the oxygen atmosphere entirely, obviously will not cause FeO to precipitate within the metal. Nor will cooling of the melt cause any precipitation, regardless of the shape or position of the "absorption curve" for any particular P_0 less than the limiting. A simple chemical principle states that the activity of a precipitate must be equal to the activity of its saturated phase.

58. For hydrogen in iron, exactly the same reasoning applies. The only difference is circumstantial, whereby the range of pressure-sensitive absorption for the Fe-O system lies below 10^{-8}, or so, atmospheres; whereas the range for the Fe-H system includes ordinary pressures and extends to some value apparently above 10^3 atmospheres. *Within its pressure-sensitive range*, either gas will *evaporate* at any surface having a deficient atmosphere until P_0, or P_H, is reestablished, but *precipitation*, in the sense of forcefully creating its own phase, is impossible.

59. In closing the discussion of this matter, a popular misconception of the same nature might be mentioned in regard to carbonated beverages in particular and the degassing of liquid solutions in general, since the same chemical laws obviously apply. The effervescence that usually follows decapping of a bottle of soda water is commonly thought to result from the loss of compression in the gas phase over the liquid. But that is a fallacy.

60. By releasing the pressure above the liquid, the dissolved gas can only be lost by *diffusion* to the surface—not by effervescence—and by silent, invisible evaporation, as has just been discussed for gas-metal systems. All the bubbles that form do so at some pre-existent surface, whether it be the wall of the container, the surface of some impurity dropped into the liquid, the surface of another bubble, or possibly the space created by cavitation if

the solution is mechanically jarred. As a matter of fact, one can judge the cleanliness of an establishment by the amount of effervescence that continues after a carbonated beverage has been poured and allowed to sit quietly a few moments! If the surface of the glass were clean and perfectly wetted, no bubbles could form there. And one should not fail to observe the bubbles forming in a glass of champagne as tiny dust particles slowly descend, initiating effervescence as they go! The slender neck of those glasses is conducive to bubbling because it is seldom washed—often purposefully. Never, in any of the ordinary gas-liquid solutions which are still pressure-sensitive, can a bubble be observed forming within the body of the liquid without nucleation.

Nucleating Factors

61.   The phenomenon of bubble formation in the case of hydrogen in liquid iron therefore comprises two factors: (1) A concentration factor, which immediately demands at any given temperature that a particular pressure P_H of molecular hydrogen be present at all surfaces of the liquid within or without; and (2) A nucleating agent which will create the surfaces within the liquid which then can lead to the collection of hydrogen and consequent bubbling.

Classification of Nucleating Factors

62.   A brief consideration of possible nucleating factors suggests three classifications:

(1)   Mechanically created surfaces, such as those caused by bubbling hydogen-free gases through the melt, stirring, or jarring;

(2)   Chemically created surfaces from other reactions, such as the carbon boil, formation of inclusions, and so forth;

(3)   Chemically created surfaces from hydrogen reaction products.

The last type warrants further comment here. Briefly, the reaction of hydrogen with another dissolved component, such as oxygen, immediately removes the sensitivity of the solubility of either component to pressure. The *solubility product,* which represents a limit as real as the true limiting solubility for either component, is then exceeded. Hydrogen and oxygen will precipitate together, creating a truly precipitated phase—H_2O.

63.   Without doubt, a large proportion of gassiness depends upon this one type of action. In copper, the dependency of porosity

upon the presence of both hydrogen and oxygen is well known. In the case of pinhole porosity in ferrous castings, the evidence supports the same reasoning[12].

Nitrogen as a Cause of Gassiness

64. In the preceding paragraphs a clear-cut case has been made for hydrogen being the principal cause of gassiness, which is permissible because it is true for ordinary iron- and steel-making. Nevertheless, even though nitrogen has been demonstrated to prevent gassiness when bubbled through the liquid steel, in certain special cases of alloy steel-making nitrogen itself can cause porosity and bleeding.

65. In making high-alloy castings, ferrochromium having a high nitrogen content is often used for grain refinement. The nitrogen in the ferrochromium is there undoubtedly as a solid phase, probably chromium nitride. During melting, the nitrogen is carried into the steel through solution of a non-gaseous phase, whereupon the dissolving is not dependent upon pressure—at least temporarily.

66. Consequently, nitrogen contents several times greater than those of air-blown steel may occur in steel made from that alloy. Problems of porosity are then almost insurmountable, so that only a fraction of the total chromium content can be added as the high-nitrogen alloy. Steel blown with air (0.78 atmospheres of nitrogen) usually absorbs less than 0.02 weight per cent of that gas, which rarely can be related to gassiness; and in the present research slightly greater than one atmosphere pressure of nitrogen bubbled through the liquid metal actually prevented porosity.

67. Chipman and Murphy[21] studied iron under approximately one atmosphere of nitrogen and found a solubility for equilibrium conditions of 0.039 weight per cent at the melting point, which would then be a rough maximum value for pure iron always lying above the absorption from air or from flushing with nitrogen. Alloying elements obviously change that value. Porosity was noted in some of their ingots, and that porosity was undoubtedly caused by nitrogen, for they did not make the mistake of numerous earlier investigators who used ammonia for studying the Fe-N system and overlooked the fact that ammonia produces three times as much hydrogen as it does nitrogen. Whether there is something like a "critical" nitrogen content for any given steel, below which nitrogen cannot cause porosity and above which it can; and

whether nucleation plays a role similar to that for hydrogen, are
questions that will be reserved for future discussion.

*Experiments Demonstrating Bleeding and Gassiness Caused by
Nitrogen*

68.   In the present research, gassiness and bleeding attributable
to nitrogen was demonstrated quite positively.   A 200-lb. melt of 5
per cent chromium steel was made in an induction furnace, using
the high-nitrogen alloy.   Analysis of other elements showed 0.34
per cent silicon, 0.73 per cent manganese and 0.11 per cent carbon.
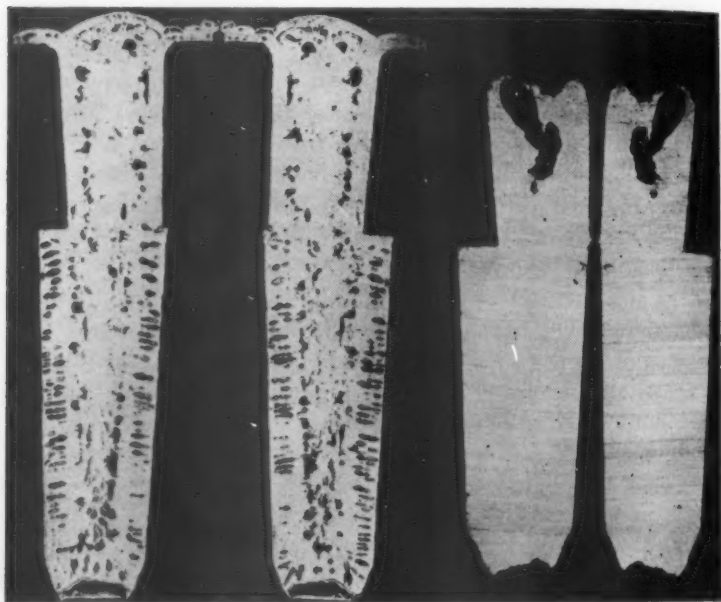
FIG. 23—CROSS-SECTIONS OF 60-LB. INCOTS OF 5 PER CENT CHROMIUM STEEL MADE WITH
HIGH-NITROGEN FERROCHROMIUM.   LEFT—ORIGINAL METAL.   RIGHT—TITANIUM ADDED
BEFORE CASTING.

Oxygen was eliminated from any important future reaction with
the small amount of carbon present by adding 0.2 lb. of aluminum
just before casting.

69.   Hydrogen was excluded from the melt by every possible
precaution; and, as a further insurance, carefully dried nitrogen
was flushed through the bath before casting in the same manner
by which gassiness had been stopped in all previous tests.

70.   Chill-cast 60-lb. ingots were poured in quick succession.

The first was given no further treatment; then two portions of titanium were added consecutively to the remaining melt, and a casting was made after each addition. The first and third ingots are shown in cross-section in Fig. 23 for comparison.

Effect of Titanium on the Evolution of Nitrogen

71. The porosity and bleeding in the first ingot, which was extreme, can only be blamed upon nitrogen, whose content was found to be 0.056 per cent. Other similar ingots analyzed up to 0.07 per cent, and even that figure must represent only the portion that remained after the effervescence was complete. The second and third ingots analyzed 0.81 and 1.39 per cent titanium, respectively. Both were perfectly solid, showing good pipe.

72. From this it must be inferred that the stability of titanium nitride has prevented evolution of the nitrogen brought into the steel by the ferrochrome. Titanium, has behaved with iron nitride much as aluminum does with iron oxide, "killing" the steel with respect to nitrogen. Under the microscope, masses of titanium nitride were visible. Iron itself, and apparently even iron with 5 per cent of chromium, which is itself a nitride former, cannot hold that quantity of nitrogen through the solubility changes occurring during solidification, and gassiness results.

CONCLUSIONS

73. The following facts regarding the phenomenon of gassiness and bleeding in "killed" ferrous castings seem fairly well established:

(1) In ordinary practice, both in iron and steel, gassiness is primarily a function of the hydrogen content and the changes in the solubility of this gas experienced during solidification. Other gases, if present in the evolution, are usually compounds of hydrogen, or are incidental components.

(2) Hydrogen alone cannot form a bubble, however. Another agent is necessary; and that agent is probably the product of a hydrogen-oxygen reaction in most cases, but may also be other reaction products, or foreign materials within the melt.

(3) Hydrogen may be introduced into the liquid metal by numerous well-recognized means, the commonest carrier

being moisture. In sand castings, one of the commonest sources of that moisture is the mold.

(4) Austenitic steels, such as nickel steels, are generally less liable to gassiness because the solid metal is a good solvent for hydrogen; in ferritic steels gassiness is aggravated because the solid metal is a poor solvent.

(5) Austenitic steels also tend to evolve less gas because the range of gamma lattice structure, a good solvent for hydrogen, approaches the solidus, either erasing the poorly solvent delta range or aiding the evolution to "bridge over" delta's narrowed position. Ferritic steels, conversely, so stabilize delta iron that the solubility decrease upon solidifying is a maximum.

(6) The addition of deoxidizers tends to enhance hydrogen absorption because hydrogen itself is a strong deoxidizer. Consequently, minor deleterious effects from additions of manganese, silicon, and so forth, may be observed because by decreasing the oxygen activity of the steel they increase its capacity for absorbing hydrogen in accordance with the simple equation for the H-O equilibrium. Minor effects from other elements conform to the same reasoning, or to the fact that some are themselves prolific carriers of moisture or of hydrogen.

(7) In certain exceptional cases, principally confined to alloy steel-making, nitrogen itself may cause gassiness and bleeding when present in excessive amount. The necessary concentrations depend largely upon the steel and the stability of its nitrides. Excessive absorption is much more easily attained when solid nitrides are added to the melt—as in the use of high-nitrogen ferrochromium—than when the melt is simply exposed to gaseous nitrogen. In such cases, the nitrogen may be stabilized or "killed" by adding a nitride-forming element such as titanium, whereupon gassiness will not occur.

ACKNOWLEDGMENT

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DISCUSSION

Presiding: H. D. PHILLIPS, Lebanon Steel Foundry, Lebanon, Pa.

Co-Chairman: F. A. MELMOTH, Detroit Steel Castings Co., Detroit, Mich.

G. F. COMSTOCK[1] *(written discussion):* This paper is very interesting and should be useful to foundrymen. It seems to the writer, however, that the distinction between absorption and solubility which, according to the authors' argument, is so fundamental as applied to the relation between hydrogen and molten steel should be explained more clearly.

We have had somewhat more experience with nitrogen than with hydrogen in steel, and since that branch of the subject is not covered as fully by the authors it may be worth while to report some additional data in this discussion. It would be very interesting to know just how much nitrogen the authors succeeded in adding to their steels when it was bubbled through the melt for ten minutes. Also, the composition of the tube which lasted ten minutes in the steel would be of interest. In our laboratory this was tried with a quartz tube, but it bent after a few minutes in the steel so that the end could not be kept down in the melt.

When we first planned some experiments with nitrogen, we were told that it could be added by bubbling the gas through the molten steel, but we had very poor results with that method. Our plain steel, melted in a small induction furnace, generally contained about 0.0040 to 0.0075 per cent nitrogen. When nitrogen was bubbled in for one minute, we got about 0.0080 per cent. As we wanted a higher content, we tried 3.5 minutes, but then got only about 0.0065 per cent. The desired higher contents were finally obtained with sodium cyanide, but we believe that some nitrogen is absorbed by molten steel from the air, and that the quantity so absorbed increases with the temperature.

The illustration of the effect of titanium in eliminating unsoundness due to nitrogen is naturally of great interest to us. This subject was also discussed in an interesting manner by Dr. G. R. Fitterer[*]. He explained how even plain carbon steel in the acid open-hearth furnace, when its silicon content is high, may absorb nitrogen which may later be evolved when the steel solidifies, causing unsoundness of the ingot or casting. Titanium or zirconium was stated to be a cure for this trouble, since they lock up the nitrogen in an inactive form so that it is not evolved in the freezing steel. This occurrence of nitrogen is somewhat similar to that which is illustrated in a much greater degree by the authors in Fig. 23, except that silicon instead of chromium was the element tending to increase the nitrogen absorption.

The same condition is probably more commonly encountered in acid electric practice. The late George Batty was one of the first to call attention to the fact that silicon nitride might be the cause of porosity in electric steel, where nitrogen is well known to be absorbed under the

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[1] Titanium Alloy Manufacturing Co., Niagara Falls, N. Y.
[*] Fitterer, G. R., *"Gases in Acid Open-hearth Steel,"* PROCEEDINGS, American Institute of Mining and Metallurgical Engineers, vol. 25, p. 245 (1942).

arc; and titanium has been often used in electric steel foundries to over-
come that trouble.

Thermodynamic data on various nitrides, published by Dr. K. K.
Kelly*, are of interest in this connection. In the following table the
stability of the compounds increases as the free energy value becomes
more negative, the positive value for the iron compound indicating that
it is unstable.

Nitride	Free Energy of Formation per Gram Molecular Weight	Free Energy of Formation per Gram Atomic Weight of Nitrogen at about 1600°C.
Fe_4N	28,600	28,600
CrN	14,000	14,000
AlN	−8,500	−8,500
Si_3N_4	−30,000	−7,500
TiN	−41,000	−41,000
ZrN	−41,000	−41,000

This shows that titanium and zirconium are practically equal in
nitride-stabilizing power, both holding nitrogen much more effectively
than the other elements. That their nitrides are not decomposed when
the steel solidifies is indicated by the presence of the characteristic
angular pink or yellow crystals in titanium or zirconium steels, as well
as by the effectiveness of these elements in preventing nitride porosity
in hot steel.

Some recent experience with nitrogen in 18 heats of killed low-carbon
steel made in our metallurgical research laboratory, most of them with
titanium additions, shows how nitrogen in steel is affected by titanium.
Each of this series of heats had about 0.2 per cent nitrogen added to
them, after deoxidation, in the form of sodium cyanide. When titanium
was used, it was added five or six minutes later in the form of TAM 40
per cent low-carbon ferro titanium, and two minutes after that the
steel was poured. The table below gives the average nitrogen contents
of the steels classified according to the size of the titanium addition:

No. of Heats	Titanium, per cent Added	Content	Average Nitrogen, per cent Soluble	Total
3	None	None	0.0143	0.0146
7	0.1 to 0.23	0.045 to 0.180	0.0039	0.0128
8	0.3 to 0.48	0.202 to 0.345	0.0017	0.0102

In a few heats the nitrogen was determined on a small sample poured
before the titanium addition, as well as on the final ingot. These re-
sults were as follows.

* Kelly, K. K., "Thermodynamic Properties of Metal Carbides and Nitrides." U. S.
Bureau of Mines Bulletin, 407 (1935).

| Nitrogen Before Adding Titanium | | Titanium, per cent | | Final Nitrogen | |
Soluble	Total	Added	Content	Soluble	Total
0.0250	0.0267	0.31	0.202	0.0021	0.0176
0.0103	0.0103	0.45	0.330	0.0009	0.0060

These results show clearly how titanium added to high-nitrogen steel, even only a few minutes before pouring, converts nearly all the nitrogen to an insoluble form which is probably harmless, and also decreases very appreciably the total nitrogen content of the steel.

JOHN McBROOM[2]: Do you differentiate between nitrogen as N_2 which, when bubbled through the bath, will wash out hydrogen, and nitrogen as essentially N or nascent nitrogen, such as possibly the nitrogen in nitrogen ferrochrome or nitrogen which may be fixed in the arc of a furnace in the presence of chrome or possibly silicon? We find that we do fix nitrogen in the arc of a furnace in heats that have 12 per cent chrome or more.

DR. ZAPFFE: The difference between the so-called "nascent" nitrogen and N_2 is simply that one dissolves in metal and the other does not. The same solubility relationships with pressure that regulate hydrogen absorption likewise regulate nitrogen absorption. Nitrogen dissolves only as the atom N, whose abundance and availability are affected by many factors. Gaseous nitrogen is expressed as N_2 because the number of free N atoms at ordinary temperatures is immeasurably small. Consequently, both hydrogen and nitrogen can be stored in steel containers without measurable loss.

At steel-making temperatures, the thermal energy breaks down many more of the double molecules, considerably augmenting the proportion of free atoms in gaseous hydrogen and nitrogen, and allowing for a noteworthy increase in solubility. That proportion may be further increased by superimposed effects, such as the electric arc Mr. McBroom mentions, since the energy of the arc tends to break N_2 into $2\,N$, the soluble form.

When nitrogen is provided by the decomposition of a chemical compound, such as chromium nitride, 100 per cent of the nitrogen that is produced is "nascent," by definition of that term. There is no atmosphere within the liquid metal, nor need one be produced by the decomposition, which is nothing other than a chemical reaction between iron and chromium nitride. No gas phase is produced, iron nitride having a very low fugacity, or "escaping tendency." Consequently, the highest efficiency for nitrogen absorption is attained in that way, though the equilibrium makes it inevitable that after a very long time the nitrogen will have progressed toward the negligible solubility corresponding to the negligible pressure of "nascent" N in the atmosphere surrounding the steel.

J. B. CAINE[3]: Regardless of how porosity is formed, oxygen plays a

[2] Hydro-Arc Furnace Corp., Chicago, Ill.
[3] Sawbrook Steel Castings Co., Lockland, O.

part in it and should not be overlooked. It must be controlled. If not, porosity will result.

Regarding flushing out hydrogen with nitrogen, this action worked very nicely in laboratory melts made in connection with a very large scale experiment. However, when it went to the shop, practical difficulties showed up, and it was not successful.

DR. ZAPFFE: The premise of the present paper, stated in the introduction, must not be forgotten. We are considering now only those ferrous castings in which the C-O reaction seems definitely excluded, at least as an important contributor to a gas evolution. But even for the gassiness attributable to hydrogen, and possibly to nitrogen, Mr. Sims and I have placed ourselves on record as believing that oxygen is also active in nucleating the new gas phase.

I would like to know the practical difficulties that showed up in the shop experiment of Mr. Caine's. Surely they can be met.

MR. COMSTOCK: What means was used to bubble nitrogen through the steel?

DR. ZAPFFE: For different metals, we used different materials. With the cast irons, a graphite proved serviceable. With the steels we tried many things, including alundum tubes, heavy-walled iron pipes, and vitreosil tubing, which served well in spite of softening.

JOHN JUPPENLATZ[1]: Twelve per cent chrome irons have a tendency toward porosity which apparently is not caused by hydrogen but by nitrogen. Aluminum does not always counteract the nitrogen if there is gas in the steel. Can nitrogen gas be eliminated by the addition of aluminum, or what additions are recommended?

MR. SIMS: Both nitrogen and hydrogen can be eliminated from steel by bubbling any hydrogen- or nitrogen-free gas through the steels. The most convenient way of doing that in practice is with the carbon boil. The bubbles start at the bottom, are all over the melt and do a good job of removing gas from the steel. In open-hearth heats, both the hydrogen and nitrogen will be at the lowest point at the end of a long boil, although they may and probably will begin to pick up after the boil stops.

In the case of steels containing alloys which cannot be boiled without losing their alloy content, some type of neutral gas would be the best means.

With both hydrogen and nitrogen, we are concerned, to a large extent, with the mechanism by which blowholes start. In a recent paper[12], we ascribed the starting of hydrogen bubbles to the reaction between hydrogen and oxygen to form a minute bubble of water vapor which is insoluble in steel and into which the gas diffuses very rapidly and causes the condition of porosity.

In cases where nitrogen seems to be largely responsible for porosity, we are not entirely sure of the mechanism involved, but believe the bubbles may still be initiated in some cases by the hydrogen-oxygen reaction. Aluminum is probably the strongest deoxidizer available to

* Lebanon Steel Foundry, Lebanon, Pa.

the steel maker but is still not a complete deoxidizer. We should think of hydrogen as a very efficient deoxidizer also, and in some unusual cases aluminum apparently cannot bring the FeO low enough to entirely prevent the hydrogen-oxygen reaction. With high nitrogen contents (usually found in high chromium steels) there is the possibility of another mechanism such as the precipitation, during freezing, of an unstable nitride which decomposes immediately with the liberation of nitrogen. At any rate porosity can be prevented in such steels by the use of titanium, which forms a very stable nitride.

MR. JUPPENLATZ: If there is nitrogen in the steel, will an aluminum addition do much good?

MR. SIMS: In some cases it will, but it may require a larger addition than would ordinarily be made.

E. C. TROY[5]: It is difficult to understand why we get more flakes in high, or 0.15 per cent aluminum-treated steel than we did with the lower, or 0.03, per cent aluminum treatment. Let us assume that we do not get pin-holes with either treatment. We used to pour our test bars with an 0.03 per cent aluminum addition, and we rarely had much evidence of hydrogen in the form of flaking or bright spots. Evidently, in neither case was the hydrogen high enough to cause pin-hole formation. Aside from the question of lowering oxygen content, unless aluminum is quite capable of fixing hydrogen, it is hard to determine why we did not get porosity. If your theory is correct, and providing we had the same amount of oxygen, there should have been bubble formation in the test bars, made from exactly the same type of steel with the 0.03 aluminum. The point is that we did not, when using dry sand cores. Neither did we get the bright flake effect that we are getting with the 0.15 per cent aluminum treatment.

DR. ZAPFFE: The porosity need not be of the pin-hole type. In fact, no porosity need result, since flakes represent the hydrogen which did not leave the metal during cooling. Was there any activity after adding the 0.03 per cent aluminum?

MR. SIMS: I believe the answer to Mr. Troy's question regarding the apparently greater prevalence of flakes with high aluminum additions might be this. With the higher aluminum additions freedom from porosity is obtained over a greater range of hydrogen contents and, with continued immunity from porosity, gradually less care is taken to maintain a low hydrogen content.

MR. TROY: I do not believe we ever noticed any activity in the dry sand core mold that we were using.

DR. ZAPFFE: Have you any idea of the comparative densities of the two metals?

MR. TROY: Only in the sense that we might compare the physical properties. In the 0.03 per cent aluminum-treated steels we got higher ductilities than with the higher aluminum-treated steels, aside from hydrogen.

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[5] Dodge Steel Co., Philadelphia, Pa.

DR. ZAPFFE: Just two points occur to me. One is that 0.03 per cent aluminum leaves more oxygen than does 0.15 per cent aluminum. Consequently, there might have been a very small amount of gas reaction which would not be observable as a blowhole, but perhaps as minute interdendritic voids. Careful measurements of the density of the metal may show that the low-aluminum metal is less dense than the high-aluminum metal. In that case, the hydrogen would not be under the high pressure necessary for flaking.

It is not usual to find flakes or shatter cracks in a casting, but if a casting containing hydrogen is forged down so the holes are welded and the gas is forged into the lattice, then flakes and shatter cracks may result.

There is a reaction in ferrous metals whenever there is present either hydrogen and iron oxide, or water vapor and iron. In other words, with iron oxide and hydrogen in contact, there has to be a reaction between the two to form some water vapor. With iron and moisture in contact, with no hydrogen, there must be a reaction to some extent with the water vapor to form some hydrogen. For every chemical system there is an equilibrium, which means a balance between two opposing reactions, which might be expressed in the following formula:

$$\frac{H_2 + FeO}{X \qquad Y} \qquad = \qquad \frac{H_2O + Fe}{A + B}$$
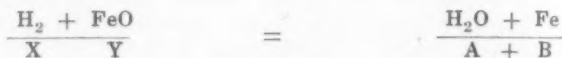
On the surface of an iron casting poured into a damp mold, there is no hydrogen; or, if there is some, it is usually below X. Consequently, the iron reacts with H_2O until it forms X amounts of H_2. On the inside of the metal, there is no H_2O. Therefore, FeO and hydrogen must react to produce H_2O in A quantity. In other words, the metal system progresses toward an end-point where it will have an atmosphere, in a pin-hole, in which there are A millimeters of H_2O and X millimeters of H_2; and wherever it starts, whichever component is deficient, the reaction will go in that direction. That is how we are able to account for pin-holes and yet have the water vapor break down on the outside of the iron.

MESSRS ZAPFFE AND SIMS (*authors' closure*): We wish to express appreciation for the discussion which has been a valuable contribution to this unfinished problem.

Making Cores for the Steel Casting†

By S. W. Brinson* and J. A. Duma*, Portsmouth, Va.

Abstract

In this paper the authors endeavor to give a simple description of the methods of making cores for steel castings as practiced at Norfolk Navy Yard. They discuss the importance of proper arbors or reinforcing rods for supporting cores, the use of chaplets and the proper material for producing the best cores. The authors also discuss coke- or cinder-filled cores vs. hollow cores, including illustrations of these and other cores, as well as of arbors, arbor bed, stamping boards for arbors, etc.

1. The foundry at the Norfolk Navy Yard can be compared with but few other foundries, in that steel, cast iron, bronzes, brasses, monel, aluminum, etc. are all made under the same roof and by the same organization. The combined output of cast iron and the non-ferrous metals will, at times, equal the steel output, and the classes of castings, such as small diesel engine castings in iron, valves, manifolds, pumps, and other pressure castings in non-ferrous alloys, present just as many problems to be solved as do the steel castings.

2. While the combination of different foundries under one roof and one organization presents many more problems than are presented to the steel foundryman alone, this is not necessarily a disadvantage. Experience obtained in solving cast iron and non-ferrous problems can sometimes be applied to a steel foundry problem, and vice versa.

3. As an illustration, one of the questions always asked of our apprentice boys is "What fundamental characteristics should an ideal molding sand have whether used for steel, cast iron or any

of the non-ferrous alloys?'' This question and the answers should apply equally as well to cores, the answers being:

(a)   A, core should be refractory enough to stand the heat and friable enough, if possible, to disintegrate after the castings have solidified.

(b)   A core should be strong enough to stand handling, pouring and running, and weak enough to give way during the contraction of the metal on cooling.

(c)   A core should be close enough to give a smooth casting and permeable enough to allow the escape of gases.

4.   These are paradoxes as the characteristics desired are in some cases directly opposed to each other in attainment. It is the problem of the foundryman, therefore, to try to obtain the happy medium for any particular casting. At Norfolk where steel castings vary in weight from a few oz. to 32 tons, this presents a sizable problem which is more easily solved by the use of scientific aids than it could possibly be by rule-of-thumb methods.

ARBORS FOR CORES

5.   In the case of any core, but more particularly a core for a steel casting, the first thing to consider is proper arbors or reinforcing rods to permit handling and supporting in the mold both before and during pouring. Any core made solidly of sand alone, when surrounded by molten metal, will have a force 4 times its own weight, tending to make it float or raise. When a core is hollow or has a coke or cinder center, this force or buoyancy becomes greater. Every foundryman, however, who has had to make a long, cylindrical, flat, or rectangular core, to be supported in the mold at each end only, and which was to be entirely surrounded by molten metal, had the same problem, in a miniature way, similar to that of the bridge builder. The foundryman does not, however, have the advantage of ''dishing'' the ''media'' to oppose the acting force and he is also dealing with a material which has practically no ductility. The bridge designer can arch his structure to prevent sagging. The foundryman also has a remedy to help support his cores, namely, a chaplet. The word ''remedy'' is used advisedly because, while it might remedy the situation in supporting the core, it may also introduce other serious shortcomings in the casting.

Chaplets

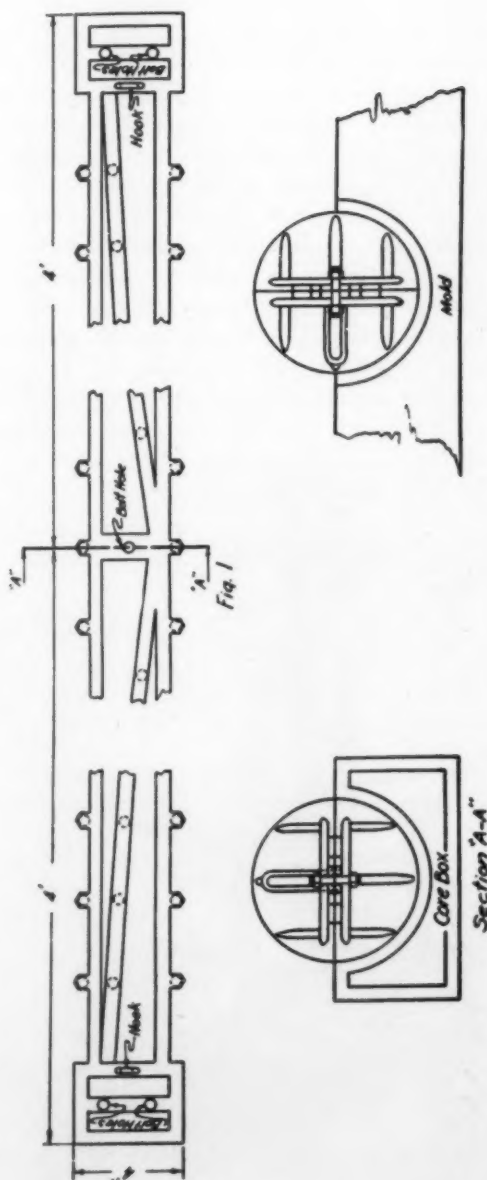6.   It is believed that foundrymen in some instances have been

Fig. 1—Plan View of Arbor for 6-In. x 8 Ft. Core. Fig. 2—Section View of Core and Arbor. Fig. 3—Core and Arbor in Mold.

too free in the use of chaplets. A chaplet is always a potential
source of trouble in producing any A-1 casting but more especially
so in producing pressure castings.

7.   While it has been reported that in some European foundries
a chaplet defect is considered a design defect, it must be borne in
mind that the designer has certain conditions to meet in designing
the casting, and it is questioned that the foundryman has always
used ingenuity to try eliminate the use of the chaplet. At Norfolk
this problem is given considerable thought, and every effort is
made to eliminate the use of chaplets in pressure castings, even
though it often would be easier and, apparently, much cheaper to
use them. The elimination of chaplets means that more thought
and care has to be given to making the core, and especially to its
reinforcing. This is of considerable importance. Latest require-
ments are that whenever any disturbance is found around a chaplet
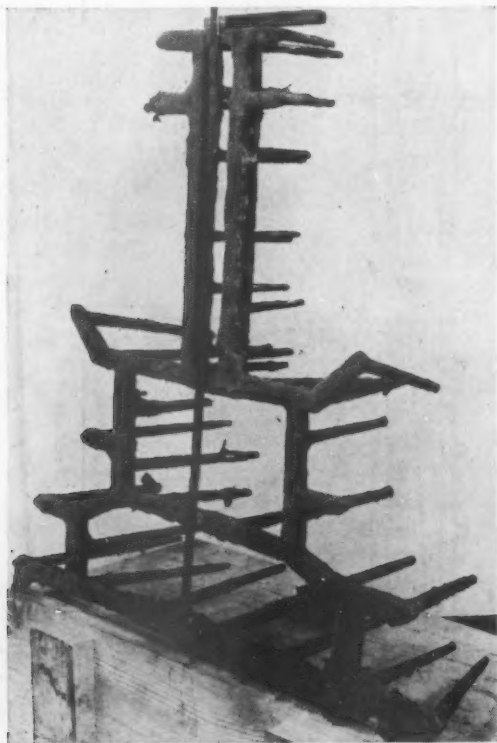

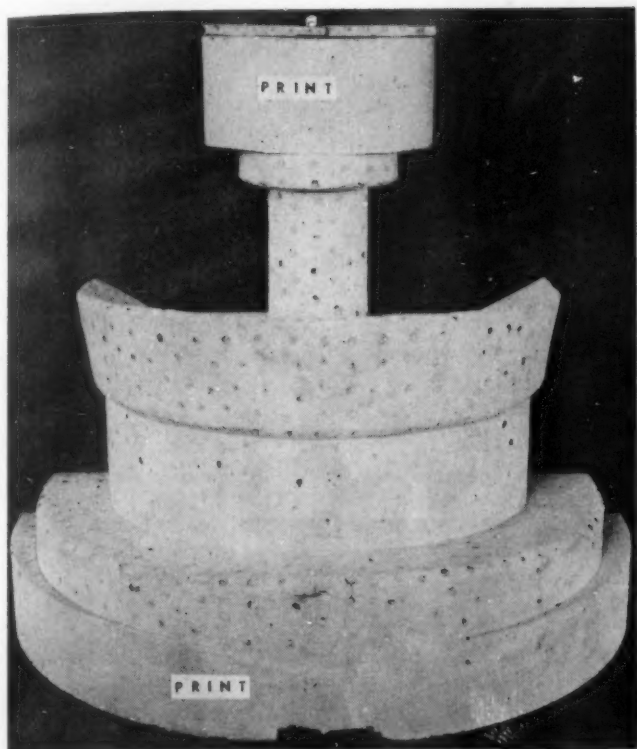Fig. 4—Arbor for Turbine Casing Core.

FIG. 5—VIEW OF CORE MADE ON ARBOR SHOWN IN FIG. 4.

on examination by radiography, it is necessary to chip out the chaplet and weld up.

Cast Iron Arbors vs. Steel Rods

8. This problem of supporting the core is such that having an iron foundry in the same plant is an aid. It is much easier to design and make a cast iron arbor, just like a bridge truss, than it would be to employ steel rods, although steel rods sometimes are used in conjunction with cast iron arbors and in small cores. On the other hand, it is necessary to guard against making the arbor too massive or heavy, as it may crack the castings due to its rigidity.

9. At Norfolk, the practice is not to make a cast iron arbor, with prongs or toes which extend close to the core surface to support the sand, square on the end. This would have an effect of crushing a block of dried sand between the casting and the arbor.

If, for example, the prong or toe had a one-in. square section, then it would have to crush a block of dried sand with one sq. in. of surface. Our practice is to bring these prongs or toes to a sharp edge like the point of a chisel which more easily breaks up the core.

10.   The prickers running at right angles to the plane of the arbor are made a little on the long side. The core maker can then very easily fit the arbor to the box by breaking off the excess length with a hammer and a short piece of pipe.

11.   The arbors are never blasted or cleaned and are at off times stored in the weather. Arbors are also stamped. The result is a rough surface which will better hold the sand and make clay washing seldom necessary.

Supporting a Long Core

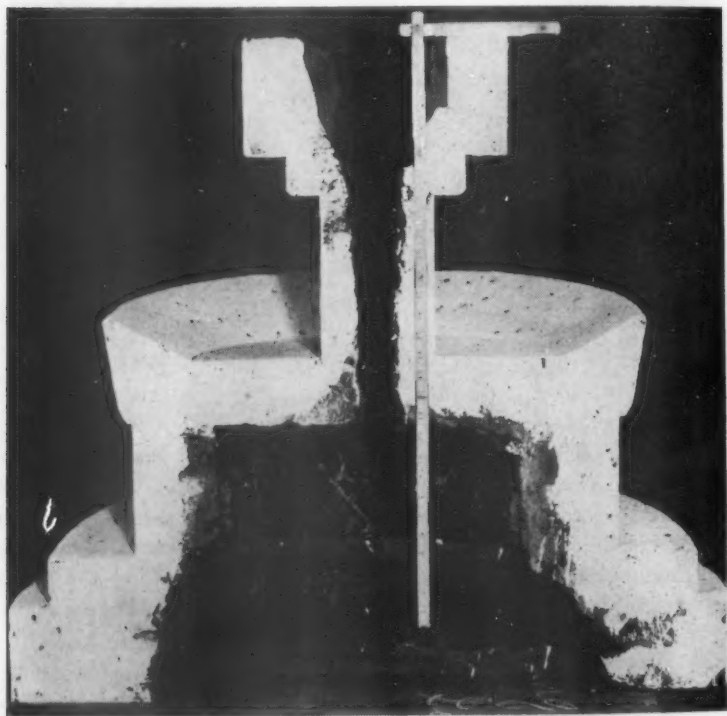12.   Previous mention is made of long cores supported only at

FIG. 6—VIEW OF INSIDE OF CORE SHOWN IN FIG. 5 SHOWING HOLLOWING AND EASE OF BREAKING ARBOR FOR COLLAPSIBILITY.
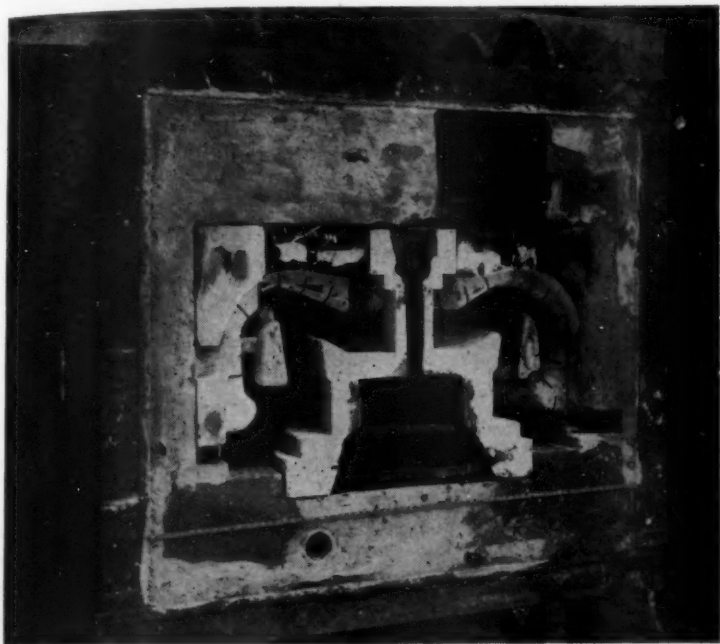
Fig. 7—Showing How Core in Fig. 5 is Placed in the Mold.

the ends, and of the elimination of chaplets in pressure castings.
Recently, it was necessary to make a high pressure pipe casting
about 6-in. inside diameter and 7 ft. long, and it was desired to
make this casting without using a chaplet. The difficulties the
problem presented can be easily realized, especially with the use
of loose rods or bars.

13. The procedure was as follows: The core was made in
halves and the arbor for each half designed as shown in Fig. 1.
One half of the core was made and rolled out on a flat plate. The
other half was then rammed, the first half picked up by means of
hooks, cast in the arbor and placed on top of the half of the core
still in the box. The two halves were then bolted together and
the entire core lifted, placed on a bed of sand and dried. Any
holes left for bolting were necessarily back filled. Figure 1 shows
a side, end and plan view of arbor for half core. Figure 2 shows
the placing of the cores together and Fig. 3 shows how the core
was located in the mold.

14. There are other advantages of the use of cast iron arbors
over the use of rods. For hollow cylindrical or half cylindrical

cores, where the hollow center of the core is accessible from outside
of the mold, the cast iron arbor can be more easily broken to permit
the core to collapse. The cast iron core, being heavier, tends to
reduce the buoyancy of the core when the mold is poured and
cleaning out is more easily accomplished in the cleaning room.
From the above it can be seen that where possible to use cast iron
arbors every job becomes a tailor-made job, cut to fit the condi-
tions.

Turbine Casing Core

15. In Fig. 4, showing a cast iron arbor made for a turbine cas-
ing, attention is called to the depth of section at a narrow part.
Figures 5 and 6 show the core made on this arbor, and Fig. 7
shows this core in the mold. It can be seen from these figures how
the core is supported. The strain that the narrow neck of the core
has to stand is obvious.

16. Figure 8 shows a turbine casing mold ready for pouring.
Note how the core is bolted up to a 3-in. machined face of special
shaped cast iron bars. The core is placed on thickness blocks in the
drag, then the cope is placed, and the core bolted on and the cope
and the core lifted together.

17. The bolt on the pouring end is removed after the job is
finally closed as this end is printed in the drag. The flask is open
between the bars all the way down to the inside of the core shell.
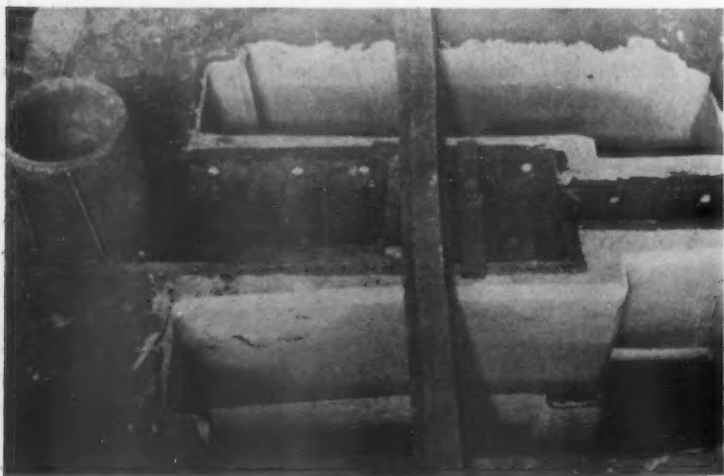
FIG. 8—SHOWING CONSTRUCTION OF TURBINE CASING CORE FOR EASE IN BREAKING ARBORS.
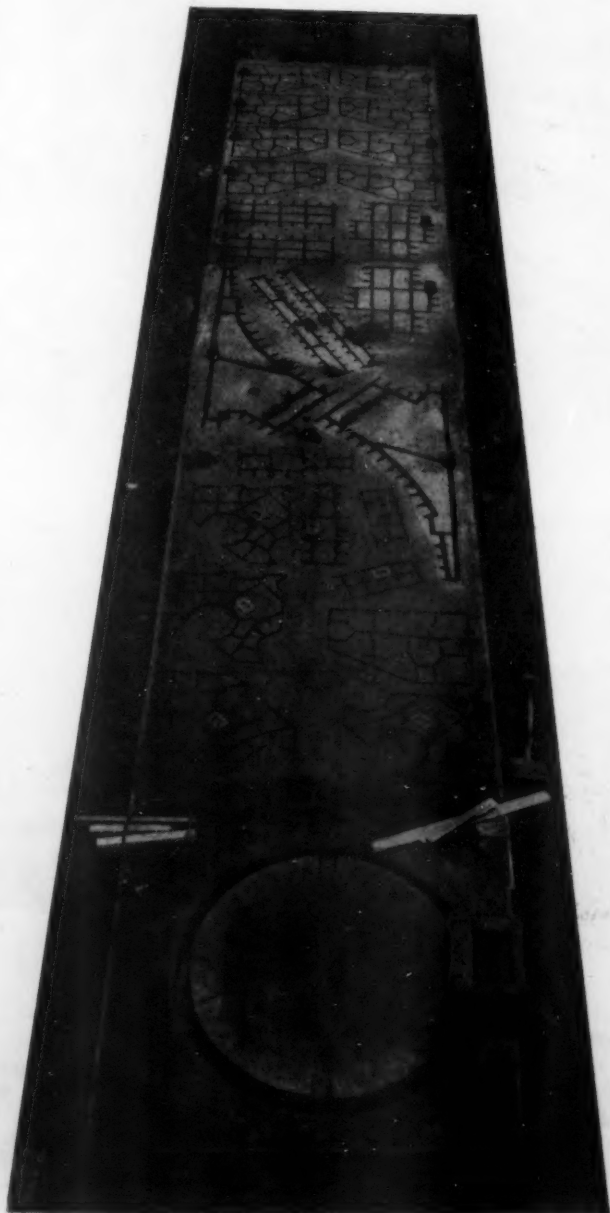
Fig. 9—Showing Arbor or "Crab" Bed Immediately Adjacent to Iron Cupola. Size of Bed—41 Ft. Long x 7 Ft. Wide x One Ft. Deep.

FIG. 10—CLOSE-UP OF ARBOR BED SHOWING HOW ARBORS ARE MADE BY STAMPING.

This exposes the cross-bars as shown in Fig. 4. At the proper time, these cross-bars are broken with a sledge and the bolts are removed from ends of the flask bars, so that the whole center is free to collapse.

FIG. 11—SHOWING STAMPING BOARDS WHEN A NUMBER OF SIMILAR ARBORS ARE TO BE MADE.

FIG. 12—SHOWING ARBORS FOR TURBINE CASING CORES.

FIG. 13—ARBORS FOR TURBINE CASING CORES. ARROWS INDICATE WHERE ARBORS HAVE BEEN BLANKED OUT FOR BLIND RISERS.

FIG. 14—ARBORS FOR TURBINE CASING CORES.

Laying Out Arbors for Cores

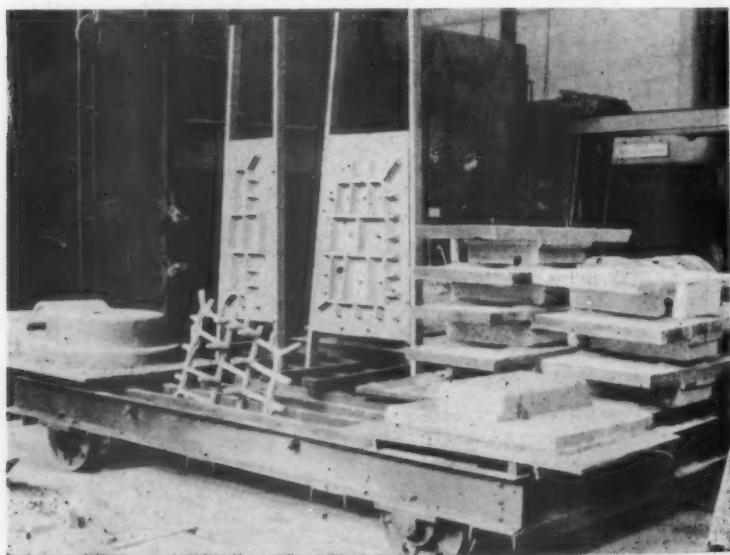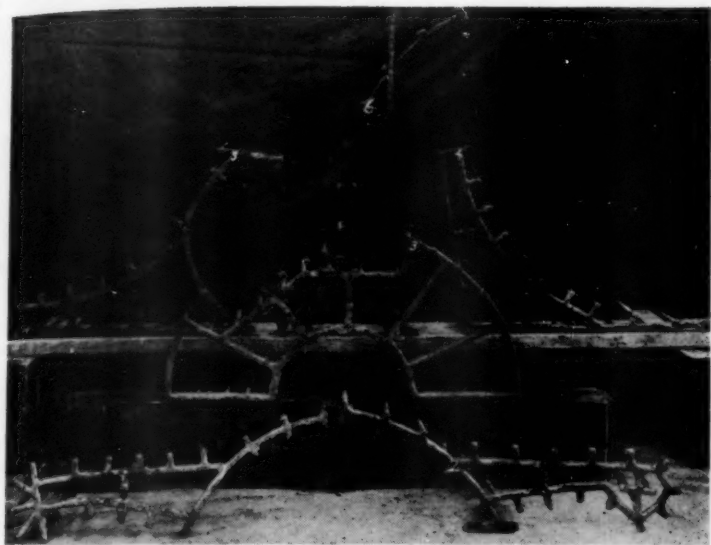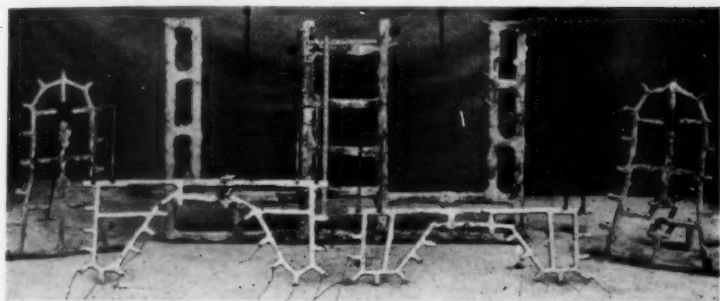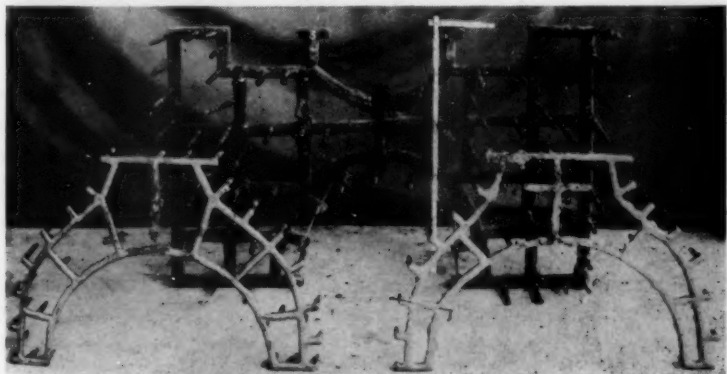18. When a core box is received in our foundry, the core supervisor looks the box over with the mold supervisor and they decide how the core is to be made. They so indicate and issue instructions to the core maker, especially trained in this work, as to how the arbor is to be made, giving location of hooks, etc. The core maker then lays a piece of heavy wrapping paper on the top of the box and, with his trowel, cuts a template. The template is then laid on the previously prepared arbor or "crab" bed and the outline of the template scratched on the sand bed. Then, with stamping and pricker tools, the core maker stamps out the arbor mold.

19. Figure 9 shows the arbor bed ready to pour off. The cylindrical mold in the foreground is an open sand mold for a cast iron piston ring. Figure 10 is a close-up of stamped arbor molds, and Fig. 11 shows how molds are stamped when there are many to be made of the same pattern.

20. Figures 12, 13 and 14 show a set of arbors made for cores for the turbine shown in Fig. 7. The arrows on Fig. 13 show where arbors have been blanked out for blind risers. Figures 15, 16, 17. 18 and 19 show the cores made on these arbors. Figures 17 and 18 show how these cores had to be sub-assembled before placing in the mold. Figure 18 is an exterior core, it being considered more economical to make as a core rather than making draw backs.

Deciding on Material for Cores

21. After the arbor to be used has been decided upon, the next question is the material to be used. At Norfolk we use oil sand, a naturally bonded sand and a cement sand. Our experience has been that a properly made oil sand core, within its limits of use, will make just about the best core for any metal. With proper thought in blending, mixing, drying and washing, it is surprising how far the limits of an oil sand core can be extended.

22. An oil sand core comes nearer filling the fundamental characteristics as laid down in paragraph 3 than any other bonded core. By blending together different sizes of sharp sands and silica flour, almost any desired smoothness and permeability can be obtained. The mixing should be thorough, and, for this purpose, the authors prefer a paddle-type mixer or light muller to the heavy mullers for core sands. Tests have shown that 5 min. mixing, after adding oil, gives excellent results. Drying oil sand cores should not be done too fast but performed in such a manner as will dry
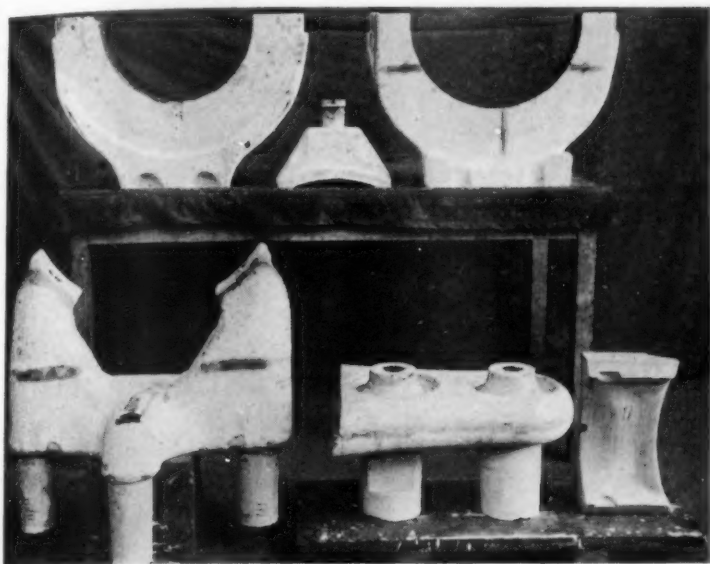
Fig. 15—Turbine Casing Cores.
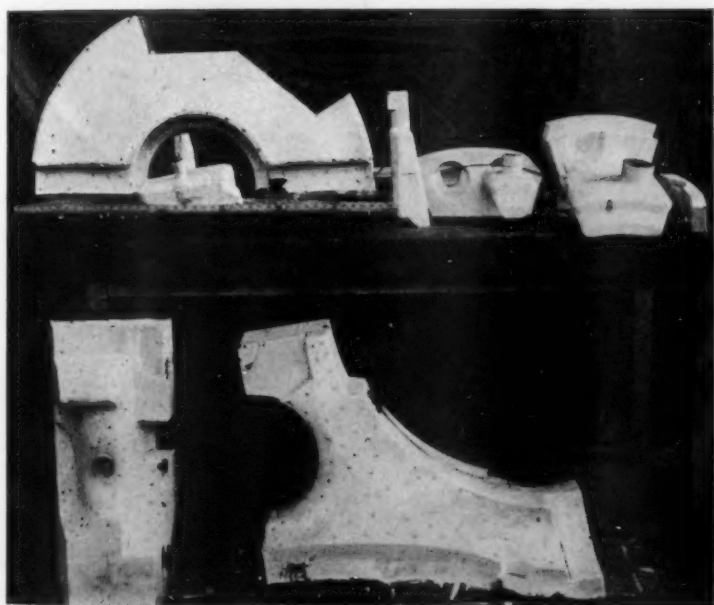
Fig. 16—Turbine Casing Cores.

FIG. 17—PRE-ASSEMBLY OF TURBINE CASING CORES BEFORE PLACING IN MOLD.

the core throughout. The reason for this is that there will be plenty of gas to get rid of without adding an additional burden of steam.

23.   Oil sand cores used in steel castings should be washed with a silica wash, sometimes as a double wash. Care, however, should be taken to see that the core surface is not too smooth or glazed as then there may be a tendency for a cleavage plane to form between the core and the wash material instead of having the wash material penetrate the core-like talons clutching the sand grains. A cleavage plane causes the wash to flake or peel off.

24.   Another advantage when using oil sand cores is that differential strength, smoothness or permeability can be obtained in the same core by using different sand mixes.

25.   Pure linseed oil is used as it has proven to be the most satisfactory core oil for this foundry.

Mixtures for Oil Sand Cores

26.   Seven oil sand mixes are being made at present for all classes of work. Of these, 3 are used in the steel foundry. The general purpose oil sand mixture is a straight mixture of 60 parts of

No. 60 washed sand to 1 part of linseed oil. For corrosion-resistant steel work, No. 100 sand is used in place of the No. 60. For heavy and thick steel castings, the mixture consists of 385 lb. of No. 40 sand, 100 lb. silica flour, 10 lb. of bentonite clay, 5 lb. linseed oil and 2 qt. cereal binder, with a moisture content of 6.0 to 7.0 per cent. This mixture resists burning down and is gradually replacing natural bonded sand in our large cores, although it is more expensive. Experiments are being made in facing the large cores with this mixture and backing with the natural bonded sand. At present, this mixture is also used as a mold facing on large jobs, especially in the cope. The face consists of about one-in. of this mixture and an additional 1- or 2-in. of natural bonded sand, with the remainder of the flask filled with the backing sand.

Sands

27. The natural bonded sand used is purchased under a specification of 90 to 100 A.F.A. fineness with a 15 to 20 per cent clay content. This sand has a higher green strength than the other
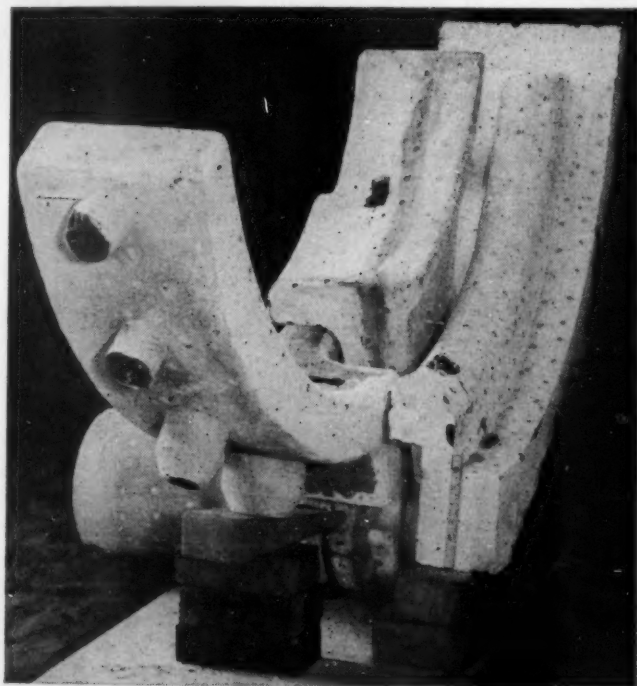
FIG. 18—ANOTHER VIEW OF FIG. 17.
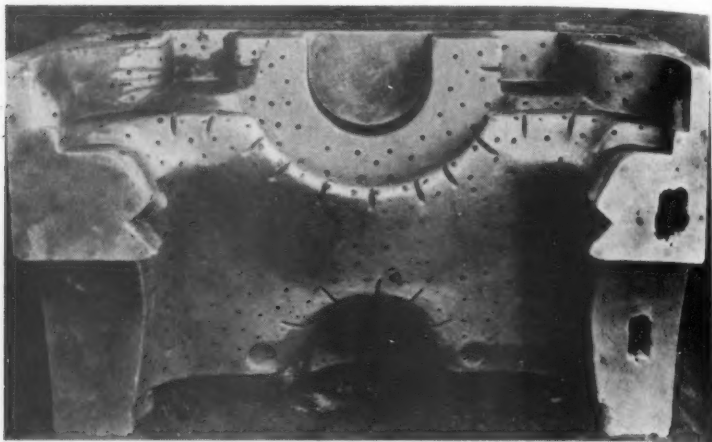
FIG. 19—EXTERIOR DRAW BACK CORE FOR TURBINE CASING.

mixtures. A particular application of this sand is for use in making large shell cores for castings with relatively thin walls. The use of this sand is being discontinued more and more in favor of the last oil sand mixture given in the preceding paragraph. Being a clay bonded sand, it has a tendency to bake like a brick in the mold and means have to be taken to relieve it.

Cement Mixtures

28. A cement mixture is used for some special applications. This mixture is 89.0 per cent No. 40 sand, 11.0 per cent high early strength cement and a moisture content of 5.0 to 6.0 per cent. At present, this mixture is being used on steel for exterior work only. On some jobs it is used principally to relieve the load on the core drying ovens. Cement work is held to a minimum because it contaminates the heap sand during the knock-out operation.

Venting

29. The question of venting is so closely interlocked with soft center cores that they should be considered together. All cores should be thoroughly vented.

30. Venting in small cores usually is accomplished by the use of wax tapers and by hollowing out. Hollowing out also helps collapsibility. According to conditions, some large cores also are made with hollow centers while others have to have a cinder- or coke-filled center to support the sand properly. These cores give good
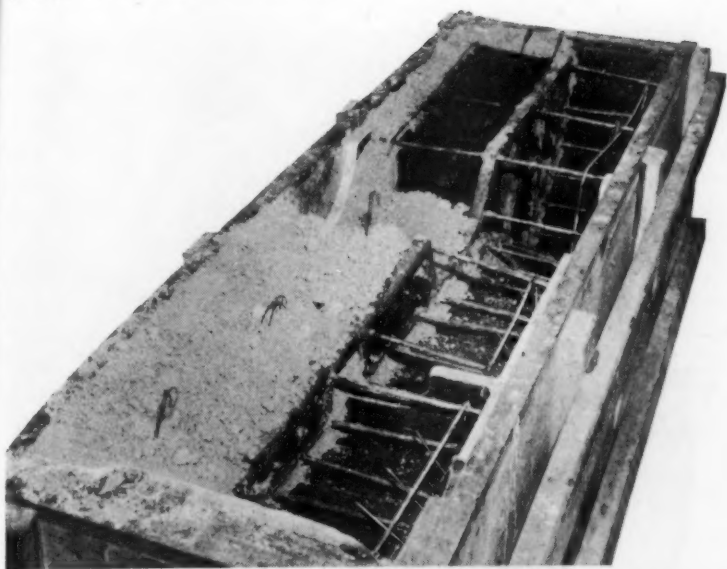
Fig. 20—Showing Large Coke-Filled Core. Internal Structure of a Submarine Stern Post Core.

Fig. 21—Completed Coke-Filled Core.

FIG. 22—LARGE HOLLOW CORE WITH ARBOR SUPPORT FOR MAIN REDUCTION GEAR CASING
FOR WINDLASS.

venting and collapsibility though the cinder-filled cores are not as
collapsible as the well made hollow cores. Hollow cylindrical cores
can, however, resist collapsibility because of the arching effect
of their shape. This can be overcome by V-notching the cores on
the inside.

Cinder-Filled vs. Hollow Cores

31.   The authors have heard old-time molders say that coke- or
cinder-filled cores were better than hollow cores because they did
not allow so much gas to accumulate inside the core. The authors
can see no basis for this conclusion. No gas leaves the inside of the
core until enough pressure is built up to force the gas through
the vents. In a cinder-filled core, this gas is forced to the outside

of the mold sooner than with the hollow core where it takes longer for the necessary pressure to build up. Thus, the hollow core is more of a reservoir and does not put as much load on the venting system. There would also be some basis for the conclusion if, when the core gas ignited, the flame would travel all the way back into the core, but this flame stops at the point the gas comes in contact with the air where there is enough oxygen to support combustion.

Examples of Cinder-Filled and Hollow Cores

32.   Figure 20 shows a large coke-filled core being made. Figure 21 shows a coke-filled core for coring the "whelps" in a large "wild-cat," the hidden side being the same as the one shown. This core is in direct compression during the contraction of the casting.

33.   Figure 22 is a good illustration of a fairly large hollow core, while Fig. 23 shows the casting in which this core is used, the wall thickness of this casting being $\frac{3}{4}$-in.

34.   Figure 24 illustrates another large, hollow core with the arbor construction, showing the long hooks for handling the core after it is put together. The casting for which this core was intended had a wall thickness of $\frac{7}{8}$-in. This core was formerly coke-filled but, due to a tendency of the casting to crack, was later
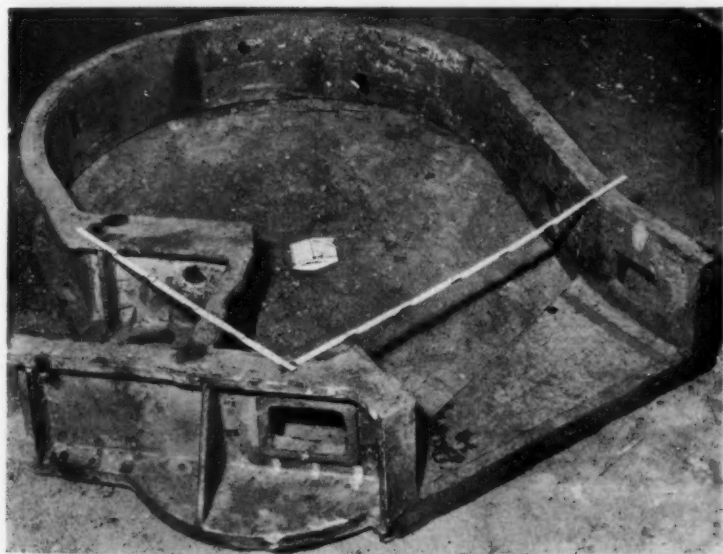
FIG. 23—CASTING USING CORE SHOWN IN FIG. 22.
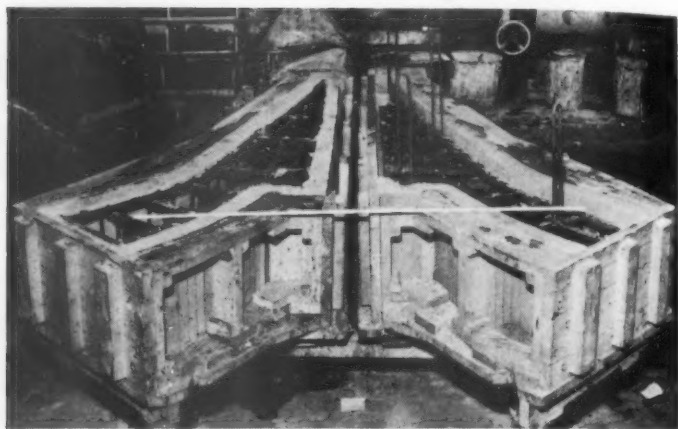
FIG. 24—CONSTRUCTION OF LARGE HOLLOW CORE WITH ARBOR SUPPORT WHEN MADE IN
HALVES.

made hollow with a 3-in. sand wall thickness. This sand wall was
considered about as thin as would work with this size core.

35.   Figures 25 and 26 show the construction of a very large,
hollow cylindrical core, 6 ft. in diameter and 6 ft. high, with the
sand wall 6-in. thick as shown in Figs. 25 and 26. This particular
core was for a cast iron cylinder liner with about 3-in. metal thick-
ness. The difference in making this core for a steel casting would
be in the sand mixture used. Figure 27 shows the results of a hol-
low core which was too strong.

Green Top Cores

36.   For pressure and other cylindrical work such as valves, fit-
tings and tubes, the general practice is to green top the cores. This
is sometimes done with rather large castings. In following this
practice, we usually V-notch the dried drag side of the core and
use dry segments on part of the prints to take the main crushing
load off the green sand top. Figure 28 shows green-topped cores
for a 15-in. cast steel gate valve and a lubricating oil pump body.
In this figure, the dried holding down segments are indicated by
A, B, C, D and E.

Anchoring Cores

37.   It was mentioned previously that, in pouring, a core has a
tendency to raise up with a force equal to 4 to 5 times its own
weight. This puts a heavy load on the prints which have to hold

the core down. Unless chaplets are used on it, it is anchored down through the bottom plate. Chaplets are satisfactory and answer every need in most work, but they are unsatisfactory in pressure casting production where we find it better to tie cores through the drag or cope rather than merely use a chaplet. Tying cores down through the drag means constructing the core in a different way to prevent crushing.

38.   Tying the core in the cope is simply a question of taking care of the weight. We have made a good-sized core for a cast steel manifold with a dry sand drag half of the core, a green top on this and dry sand branch cores setting on the green sand top, with all of the cores tied down through the drag. This core is shown in Fig.
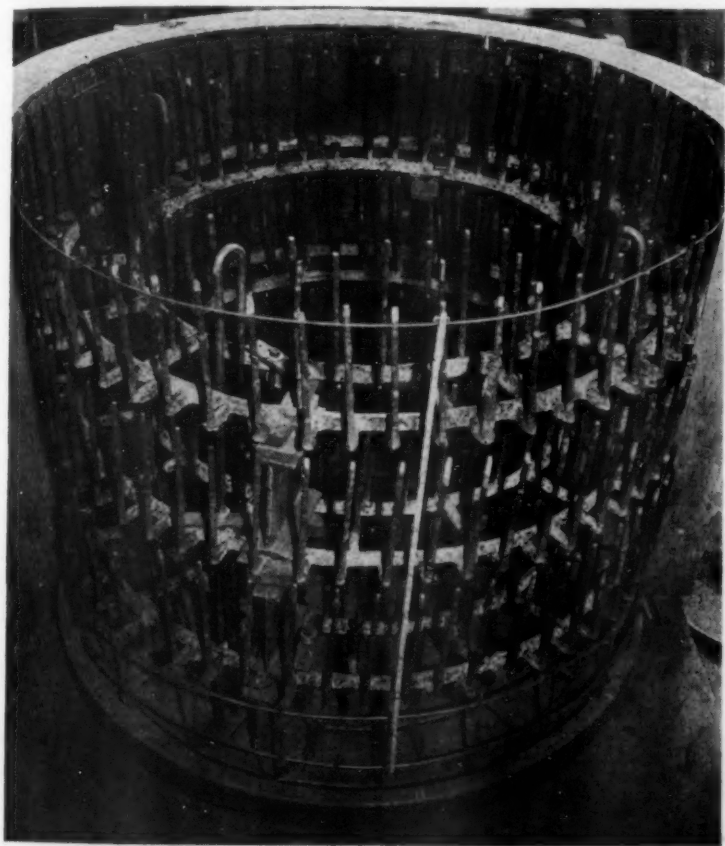
FIG. 25—ARBOR OF A VERY LARGE HOLLOW CORE.

FIG. 26—VERY LARGE HOLLOW CORE USING ARBOR SHOWN IN FIG. 25.

29. The dry sand bottom cores on which body prints should be noted. We started from the bottom plate, packing with short pieces of flat steel bar through the bottom cores, the dry side drag half of the body core, through the green sand top, up to the print of the top dry sand branches. By putting a hook in these branches and passing down through the other cores we had a metal foundation from dry sand branches right down to the bottom plate with no fear of crushing the lower cores. Figure 30 shows the body and bottom cores. Fire brick is also used for packing purposes.

Nailing and Washing

39. After the core is made, the next question that arises is that of nailing and washing the core. We use nails wherever we feel it is necessary, depending on conditions, but we always protect the core in sections which are right in front of the gate, nailing heavily at this point.

Washes

40. Washing cores is one of the most important items. A wash gives the core a more refractory coating and promotes a smoother casting. In washing a core it must be kept in mind that the permeability is somewhat reduced. The wash used at Norfolk is as follows:

    2 qt. bentonite
    2 qt. cereal binder
100 lb. silica flour
  20 gal. water

The cereal binder and the bentonite are first mixed dry and then mixed with 10 gal. of water. This mixture is then allowed to stand for 24 hours to allow for the swelling of the bentonite. After standing for 24 hours it is then put in the mixer with the silica flour and the rest of the water. The wash is agitated continuously. Some time ago an old dough mixer was obtained and a faucet welded in at the lowest point, the shaft packed and a piece of wire mesh was placed over the top for safety. This machine can be seen
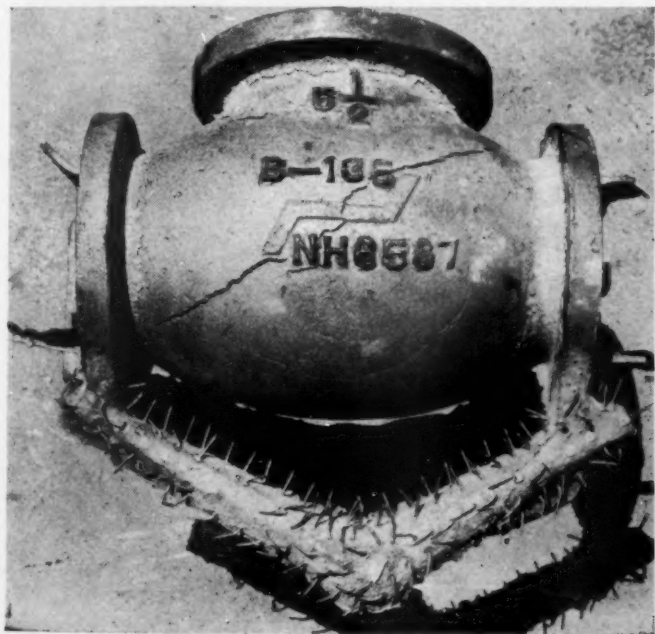
Fig. 27—Showing Cracked Casting Due to Core Being Too Hard.
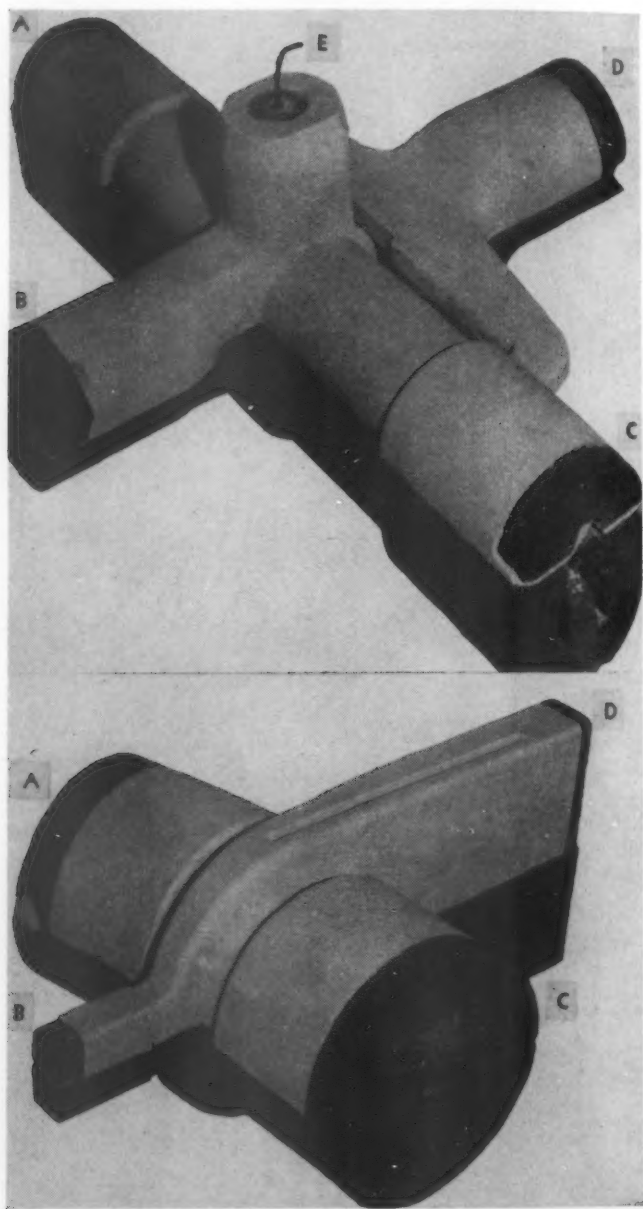
FIG. 28—SHOWING GREEN-TOPPED CORES FOR 15-IN. GATE VALVE AND OIL PUMP BODY
WITH DRY SAND HOLDING DOWN SEGMENTS.
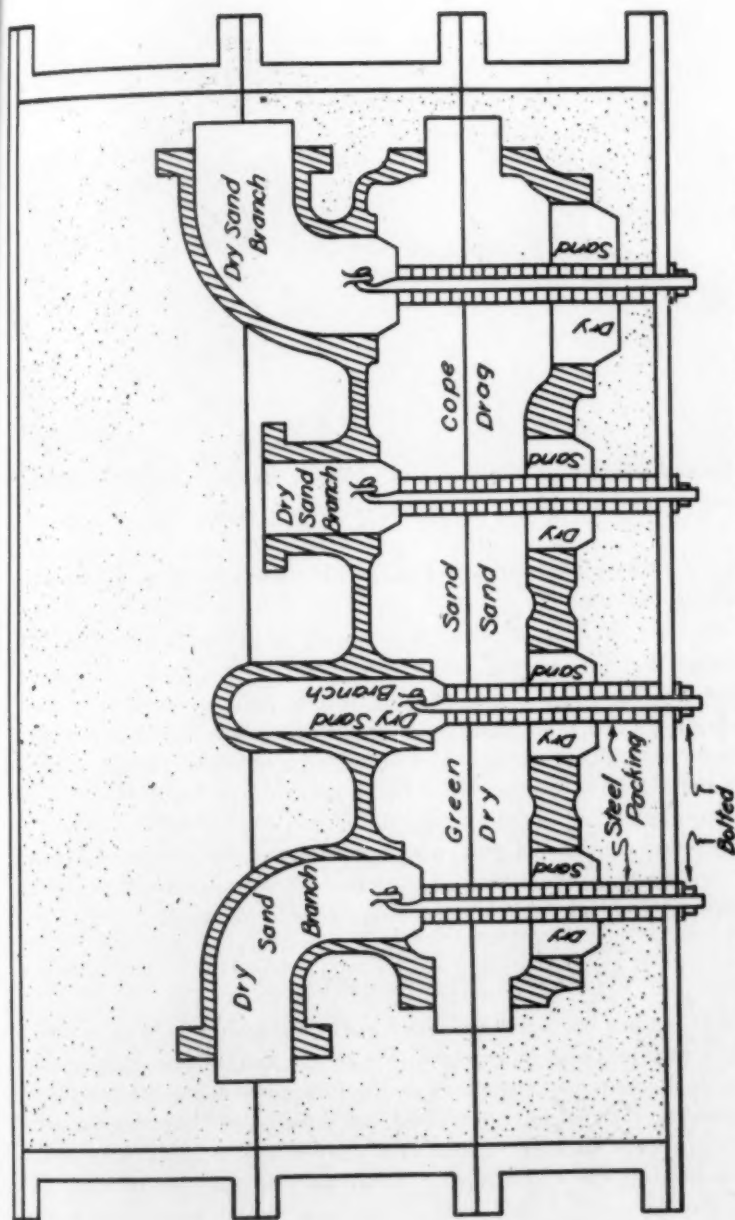
Fig. 29—Showing Method of Construction of Combination Core for Anchoring Through Drag.
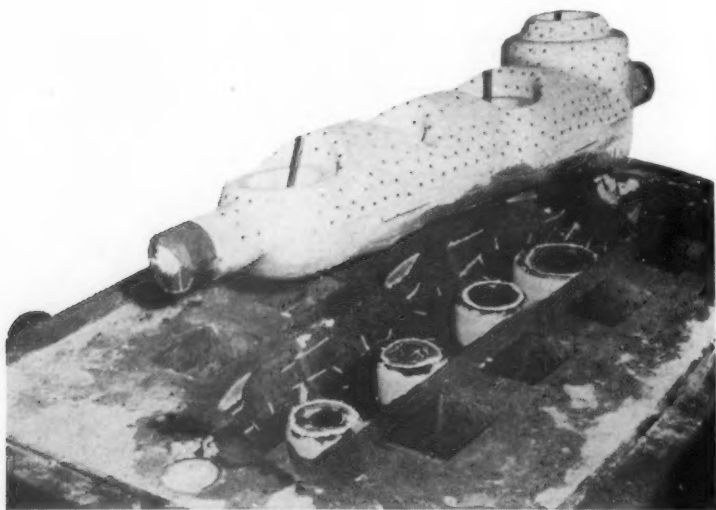
FIG. 30—CORE ASSEMBLY FOR THROTTLE VALVE BODY MADE AS SHOWN IN FIG. 29.

in the right background of Fig. 24. It makes an ideal machine for mixing the wash.

41. At one time, linseed oil was used in the wash but it was concluded that this was not best suited to our conditions. It was felt that the linseed oil (especially on core mixes containing clay) caused a wash penetration to a certain point where a cleavage plane would be formed. Also it would not allow the penetration of the wash in tentacle-like form around the sand grains. The dropping off of large flakes, all of a uniform thickness, on flat copes was convincing evidence that this was the penetration depth of the wash, as there was a sharp line of demarcation between this penetrated depth and the rest of the core or the mold.

SUMMARY

42. It has been our endeavor in the foregoing to give in as simple a way as possible a description of the methods of making cores for steel castings as practiced at Norfolk, presenting the reasons for following the methods described. It is believed that the reasoning is logical but in some instances it may not so appear to everyone. It is only by the discussion of differences of opinions in a fair and square way and in an honest effort to arrive at some fundamental conclusion that we expand and grow.

ADDENDUM

43. Since the foregoing paper was prepared we have received orders for what appears to be the largest hawse pipes ever cast in this country. The pattern is being made in a shell form, with the core to be rammed up in one piece inside the pattern. It offers one of the biggest problems we have had in core supporting and arboring.

DISCUSSION

Presiding: A. H. JAMESON, Malleable Iron Fittings Co., Branford, Conn.

Co-Chairman: H. H. BLOSJO, Minneapolis Electric Steel Co., Minneapolis, Minn.

CHAIRMAN BLOSJO: I should like to ask Mr. Brinson if he feels that it is possible for a jobbing foundry to meet the present O.P.A. prices, using this type of arbor? It is a big expense to the foundry to equip for this sort of thing and, with the present prices, I wonder if the ordinary foundry can make use of them.

MR. BRINSON: I do not know whether this is a question for a foundryman or for somebody from the O.P.A. However, where we use cast iron arbors, you can accomplish the same thing with steel rods welded, except for collapsibility. I have seen that done recently, but I still go back to something I said several years ago, at an American Foundrymen's Association meeting, and I repeat it: there is nothing as expensive as a rejected casting.

If you can make cores so that you do not have any core breakage, cuts through, or a lot of dirt and holes in the casting, it is all right. Any steel foundry without an iron foundry available may be able to put in a small melting unit. We sometimes make ours in a brass crucible furnace, or a small electric furnace as may be available.

As for the expense, I think that is rather low. Where it can be utilized, the expense is probably less than it would be with any other kind of arbor. It certainly provides the best chance to break the arbor down, to collapse the core, and keep it from breaking the casting. We have had castings that, I know, would have caused trouble, if we had not been able to do that, which makes it an important point to consider.

I started by stating that the problem of having to make steel, cast iron, a dozen different mixtures of brasses and bronze, aluminum, monel and every special mixture imaginable, under the same roof, is a kind of headache and an awful handicap. Being able to make cast iron arbors is one item that helps to compensate for some of those handicaps, and everybody cannot be unfortunate all of the time. It may not apply to a great many foundries but, as far as making arbors is concerned, you could do it with a flat bar. Instead of laying it horizontally, put it on edge—you will get more strength in that direction. With these arbors, we frequently put a piece of steel rod down in the "crab," or arbor, bed and cast the iron around it.

Another advantage we have in the cast iron arbors is when the job is cleaned out. We break these arbors, and it is much easier to get the core out of the casting, especially one like a hawse pipe—or any other cylindrical casting—and then we throw the broken arbor back into the steel or iron melt. We simply melt it over and over again.

W. H. PARKER[1]: You mentioned making a cast iron arbor, to prevent the cracking of the casting. How do you get to it, if it is a steel casting, to break it down? Have you some special method of getting to that arbor?

MR. BRINSON: The core is open. In other words, on the turbine casing, we cast two special cope bars, and the only thing that is in the cope are the risers. The cores are set down on blocks which are of the same thickness as the casting. The cope is set down on them, with the bottom 3-in. face of the bar resting on cores. That is set right down on the drag, with the cores in it, and the cores are bolted up to this cope, the face of the bar to the face of the core, and then the whole cope, itself, is lifted, with the cores hanging on. That gives us the proper thickness of the steel.

It is hollow in between those two iron bars, and you can go all the way down to the bottom of that core, which also is hollow, to within 4- to 6-in. of the casting. After the casting is cast, we break up the cross bars in the arbor and loosen the two special bars by burning the holding bolts in half so that there is nothing to hold the casting and keep it from shrinking in towards the center. The whole unit is loose. In other words, we always make provision in a job like that to get to the inside when we break it.

MR. PARKER: How would you handle it if that core were completely surrounded with metal, excepting the prints, like a runner casting or something of that nature?

MR. BRINSON: We would break it from each end, just take the end of the flask off, or a couple of pieces of the flask off, and go in there with a bar and break it.

MR. PARKER: You would not be able to do that with a steel arbor.

MR. BRINSON: That is right—that is what we consider one of the advantages of a cast iron arbor.

MR. PARKER: What composition do you use for the top of those green top cores, what mixture?

MR. BRINSON: I believe it is the green sand mixture, the same as used in the mold. We use a local sand mixed with bentonite and about 3.5 or 4 per cent moisture.

MR. PARKER: You use the bentonite as the bond, and you do not do any skin drying at all?

MR. BRINSON: The bentonite is the bond, and there is no drying. Simply set it in the mold as it comes out of the core box.

MEMBER: What grain size of sand is best for this type of large work?

MR. BRINSON: The natural bonded sand, of which most of these large cores are made, runs about 90 to 100 grain fineness, with around 15 per

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[1] Continental Roll and Steel Foundry Co., East Chicago, Ind.

cent clay. A straight oil-sand mix will run about 1 to 60, oil to sand. For the larger cores, we use the no. 40 grain size, but for the smaller ones we probably go up to 60.

CHAIRMAN BLOSJO: In one mixture, given in Mr. Brinson's paper, I remember distinctly that he used around 400 lb. of sand, 10 lb. of bentonite, some 100 lb. of silica flour and 4 lb. of linseed oil. Our experience has been that, with such a large proportion of bentonite, 5 lb. of linseed oil would give a very weak core.

MR. BRINSON: I do not recall, offhand, where that mixture is or to what it applies. As I recall, that is the mixture we used. However, it is more or less of an experimental mixture up to the present time. Most of these large cores are made from natural bonded sand but, due to the fact that we sometimes get natural bonded sand that is very wet, we have been trying to go to a synthetic sand which would give us the same result.

MR. PARKER: Do you think a synthetic sand would give you a better oil-sand core than the natural bonded sand?

MR. BRINSON: For the oil-sand core, yes. We use a washed sand and oil sand mixed. We do not put anything in the natural bonded sand; it is just milled.

MR. DUMA: I have a mixture here for the strong valve core.

MR. BRINSON: This particular mixture, to which Co-Chairman Blosjo refers, is a mixture that we are using in molding as well as trying it on the cores. This is used in the facing, where we have had trouble with other sands, and we just thought of trying to use it in the cores at the time this paper was being written. We do not know whether it will be strong enough to stand up in the cores. However, in the case of a facing for a mold, you can see the sand will not have to be nearly as strong as it has to be in the core, so it is to that particular application that we have reference.

This is a test sheet of this particular mixture, and the large hawse pipe core was made from this mixture. It shows the same thing here: 385 lb. of Cape Henry sand, 100 lb. of silica flour, 10 lb. of bentonite clay, 5 lb. of linseed oil, and 2 qt. of cereal binder.

MESSRS. BRINSON AND DUMA (*authors' closure*): This paper being mostly descriptive of actual practice rather than argumentative would necessarily be productive of questions for further information rather than extended discussion. The one point mainly brought out, however, has been the question of arbors for large cores.

There can be no question of the fact that if a core is too solid or strong it will crack the casting. In large cores it is essential that they be reenforced with arbors to make them strong enough to be handled safely in both the green and dry state, and to prevent sagging when placed in the mold, and to prevent bucklings and crushing while and immediately after the mold is poured. However, the very properties added to the core by the arbors prevent its free collapsibility, and after solidification has started they become a menace to the production of a sound casting. As is often the case in foundry work the foundryman

finds himself between the "Devil and the deep blue sea." If he makes the core too strong he may crack his casting; if he makes the core too weak he may loose the casting on account of sagging, breaking or buckling. This is the reason we feel that cast iron offers the best material for a large core arbor. It gives all the strength needed up to the time the casting starts to shrink, and at that point it can be readily broken down to give free collapsibility and prevent the cracking of the casting.

We have cast iron available, so the use of cast iron arbors offers no problem. Plants which do not have cast iron available must decide for themselves whether the advantages derived are worth the cost of putting in cast iron melting equipment, the cheapest of which would be a simple tilting crucible furnace, such as is normally used for brass, or one of the very small tilting cupolas.

Core Blowing as a Factor in a Semi-Production Foundry[†]

BY Z. MADACEY[*], PEORIA, ILL.

Abstract

In this paper, the author discusses methods of core blowing and related factors at Caterpillar Tractor Co., citing as some of the factors necessary for successful core blowing, accurate sand control, skilled help and cooperation between every department in the organization involved in core blowing. He describes core blowing equipment and various large and small cores, giving illustrations of same. He tells of procedure and results of experiments conducted on the production of aluminum aircraft cylinder heads.

1. The interest of Caterpillar Tractor Company in core blowing began several years ago. Notwithstanding, the personnel of the foundry division has been familiar with this practice for many years. It was, for some time, thought that in order to make core blowing a successful and paying operation, it was necessary to have large production orders. At that time, due to the variety of cores produced in the foundry and the constant changes in jobs involved, the company, at first, approached core blowing on an experimental basis.

2. One can visualize the constant changing that must take place when there are many thousands of patterns that are subjected to being in the sand at some time or other, with the required quantities on a monthly basis relatively small as compared with the large production of many specialized plants.

3. Experience has now demonstrated that it is not necessary to be one of the select few of highly specialized production foundries in order to produce economically, quantity and quality cores of all shapes and sizes by core blowing. There are, at present, numer-

ous semi-production foundries reporting an increased percentage of cores made by the blowing method.

PNEUMATIC CORE BLOWERS

4. It has been our observation, through years of experience, that successful practice in blowing cores consists of several major factors, and it is our purpose to discuss in this paper these factors in the order of their importance. Figure 1 illustrates a battery of smaller type core blowing machines, conveniently arranged to take up a minimum amount of floor space, leaving ample room for operating efficiency. Due to the variety of light, medium and heavy castings made, it became necessary to equip each of these machines with individual sand containers to insure accurate distribution of sands to the job for which each was originally intended.

ACCURATE SAND CONTROL

5. One of the many factors involved is accurate sand control. There are several well known reasons why moisture plays an important part in the blowing of cores.

6. *Moisture and Wear of Core Box:* Sands having a moisture reading of less than 1.8 per cent are considered dry and detrimental to both cores and equipment. The core made of sand which is considered dry will not hold the shape and size required. Such sand will adhere to the core box due to the fact that water, at this stage of the operation, acts as a binder, cementing together the individual sand grains and the finer the sand, the more pronounced this condition becomes.

7. The air intended to move a volume of sand when the blow valve is released does not move dry sand in the required volume. It blows through the sand, carrying into the box cavity only a portion of the intended amount of sand, setting up a drastic cutting action. Under such conditions the oil in this first spray of sand entering the box oxidizes very rapidly from the rushing stream of air and naturally follows the displacement of air caused by the air vents and the joints of the box. Having done this damage to the box by the cutting action, the oxidized sand wedges in the vents between loose pieces and, in some cases, between the joints of the driers. Then any additional air entering the box cannot pass through. The air, becoming compressed, fills the cavity in the box, making it impossible for any additional sand to enter. Also, the
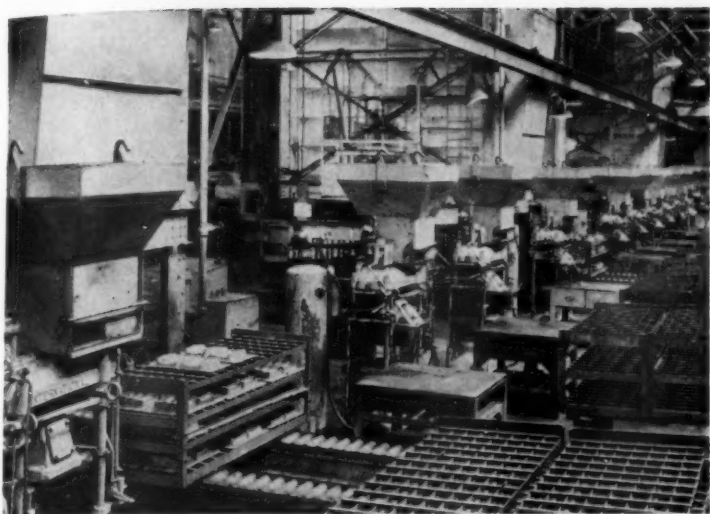
FIG. 1—BATTERY OF CORE BLOWING MACHINES OF THE SMALLER TYPE CONVENIENTLY AR-
RANGED TO TAKE UP A MINIMUM OF FLOOR SPACE, LEAVING AMPLE ROOM FOR OPERATING
EFFICIENTLY. RACKS USED FOR UNLOADING CORES FOR BAKING ALSO SHOWN.

sand which has wedged in between loose pieces and driers causes
them to stick when the box is drawn.

8. If an attempt is made to use sand which is considered dry,
yet not quite dry enough to prevent blowing, considerable wear can
be noticed on the core surface of the box after some 200 to 300
cores are blown. Directly underneath the blow-holes, boxes have
been known to wear as much as 1/32-in. in about 3 hours.
Hardened steel inserts will prevent wear directly underneath the
blow-holes, but will not prevent wear on parts in the path of the
moving sand.

9. *Moisture and Soft Rammed Cores:* Oftentimes soft rammed
cores are attributed to the lack of vents or blow-holes, and, in some
cases, to both causes, when in reality the condition or moisture con-
tent of the sand is at fault.

10. Moisture readings above 1.8 per cent for core mixtures for
our smaller type machines and above 3 per cent in our large ones,
are considered wet. Sand with higher moisture reading can be
used with considerable difficulty. The excess water is blown from
the individual sand grains when the blow valve is released and
forms a pool underneath the blow-hole, causing the sand to stick
to the box. Cores blown with sand of a high moisture reading will
also sag and lose their intended shape, the same as soft rammed

cores made by hand or jolt machines. Excess water also breaks down the cereal binder which is used for green bond. It has been our experience that moisture limits for sands for small blowers must be held between 1.8 and 2.2 per cent and on large blowers from 2.5 to 3.5 per cent. We have found that the shallower the core, the more closely the moisture must be held to 1.8 per cent.

11. *Moisture and Baking:* It is logical to assume that two cores containing different amounts of moisture are not going to bake alike. If one core is baked to perfection, the other is either over-heated or it retains some of the moisture contained after mixing. We realize that when molten metal comes in contact with moisture in a core surface, it causes the metal to boil. The water vapor formed is so much greater in volume than the original water that undesirable results occur. Soft cores caused by overheating in bak-ing cannot withstand the rush of molten metal and sections are easily washed away.

12. *Iron Oxide:* Data on a core sand mixture used in core blowers for manifolds, cylinder head jackets, valves and miscel-laneous cores made with and without iron oxide (FE_2O_3) are shown on Table 1. Two per cent iron oxide is used to good advan-tage in blower sands where the castings made with it show an ex-

FIG. 2—EXAMPLES OF CORE BOXES AND DRIERS FOR CORE BLOWING.

cessive number of vanes or "rat tails," as they are commonly referred to in foundry practice.

SKILLED HELP IMPORTANT

13. Skilled help is a factor of which we cannot lose sight. Supervisors know which are their important jobs. These jobs usually require the most skilled and dependable workmen and each supervisor should always have an understudy for each of these, so that the department may continue to operate economically, and at full production, when any one or more of the key men drop out for any cause whatsoever. All experienced supervisors know that each core box has its own peculiar characteristics that require attention, so if skilled operators are not available, the supervisor's problems have increased, particularly so in a shop where a large variety of cores are made.

14. We wonder how frequently core blowers are purchased with the thought in mind that any one capable of pulling a few levers can, in a very short time, be trained to become an efficient operator. Prior to the purchase of core blowers, considerable time should be spent in training efficient help, and naturally, with a sufficient number of skilled core makers, these men will prove to be the best qualified and the most skillful blower operators. Such men will also realize the importance of caring for equipment and machines, and quickly notice slight defects in equipment and report them

Table 1

COMPARISON TESTS OF CORE SAND, WITH AND WITHOUT IRON OXIDE, ALL OTHER FACTORS REMAINING THE SAME

Sand Mixture	Mesh No.	Screen Test of Base Sand — Per Cent Remaining on Mesh
50 Gal. Silica Sand	12	0.1
30 Gal. Lake Sand	20	0.1
20 Gal. Bank Sand	30	6.2
7 Qts. Oil, or, 1 of Oil	40	32.0
to 58 of Sand by Volume	50	20.6
	70	21.0
	100	10.0
	140	4.0
	200	3.0
	270	2.3
	Pan	0.2
	Total	99.5

A.F.A. Grain Fineness No. 51.2
A.F.A. Grain Class No. 5

Table 1 (Continued)

COMPARISON TESTS OF CORE SAND, WITH AND WITHOUT IRON OXIDE, ALL OTHER FACTORS REMAINING THE SAME

	With 2 Per Cent (FE_2O_3) Iron Oxide			Without (FE_2O_3) Iron Oxide		
	Green Compression (lb. per sq. in.) 1.2			Green Compression (lb. per sq. in.) 0.85		
	Permeability (A.F.A. No.) 170			Permeability (A.F.A. No.) 180		
	Moisture (per cent) 1.7			Moisture (per cent) 1.8		
Baked at	400°F.	450°F.	475°F.	400°F.	450°F.	475°F.
Dry Compression, lb. per sq. in.	160	400	160	440	420	360
Permeability	200	200	200	210	210	210
Tensile, lb. per sq. in.	29	48	28	48	112	58
Collapsibility at 2000°F., min.	9	12	12	2½	5½	3½
Collapsibility at 2500°F., min.	4	12	12	2½	3	2½
Expansion at 2000°F., in.	0.010	0.018	0.019	0.004	0.008	0.016
Expansion at 2500°F., in.	0.009	0.011	0.009	0.009	0.008	0.013
Expansion time 2000°F., sec.	120	120	60	15	15	45
Expansion time 2500°F., sec.	15	45	30	90	15	30
Contraction at 2000°F., in.	0.008	0.018	0.012	0.009	0.012	0.008
Contraction at 2500°F., in.	0.019	0.063	0.069	0.014	0.013	0.007
Maximum Contraction time at 2000°F., min.	9	12	12	2½	3½	3½
Maximum Contraction time at 2500°F., min.	4	12	12	2½	3	2½
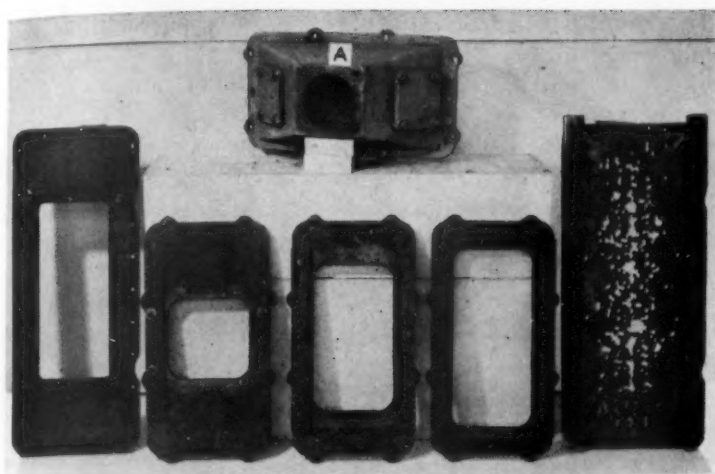
FIG. 3—SAND RESERVOIR (A) AND MAGAZINE PLATES.

to the supervisors before they require costly repairs. Experienced core makers also take considerable pride in the quality of their work and are able to assume a reasonable amount of responsibility for their own safety.

COOPERATION

15. Of the many management factors involved, the one considered most important, in fact essential, is cooperation. This factor covers a wide scope; every department in the organization is in some way involved. There are numerous ways in which the molding departments, pattern shop and cleaning room can contribute in simplifying and making core blowing more economical. To mention briefly a few, gating in some cases can be successfully changed to accommodate the core room; patterns can be turned over, making the original cope the drag. It is also economical at times not to plug impressions left by wire and rod stools as the metal that forms in these impressions can readily be removed in the cleaning room by the sweep of a hammer.

16. The pattern shop supervision can, of course, appreciate the core room's problems. Being so closely associated with the core department, it can anticipate the difficulties that are often encountered, particularly with new core equipment, and pattern shop supervision can, in many cases, discuss these with the engineering division and obtain changes to overcome the difficulties.

17.   The results of pattern shop interest in one of these cases is illustrated in Fig. 2.   It is apparent in this example that the engineering division had specified two cored holes (*A* and *B*) in the casting to be made from the equipment on the top shelf. Two core prints were provided for in the dryer. Pattern shop supervision, realizing the difficulties that would be encountered with these prints on a core as bulky as this, conferred with the proper authorities with the result that the holes were removed. These holes are now drilled instead of being cored out. Blowing of the prints, wiring, care in stacking and transporting have all been eliminated. This example also brings out the fact that core boxes containing several loose pieces can be successfully blown.

RIGGING BOXES FOR CORE BLOWING MACHINES

18.   It is not our intention to present any hard and fast rules, which must be practiced when blowing cores. The amount of air pressure available in any foundry, for instance, will determine the number of vents, blow-holes, and their sizes. Sand-drying facilities will also, to some extent, be a determining factor. It has been our experience that, although there are certain fundamentals or basic principles to which we should adhere, the supervisor should possess and exercise good imagination and ingenuity.

19.   It is only natural that in the course of time we do encounter numerous core boxes with a striking similarity. In these cases, our basic principles can be applied. In most cases, however, each box, or set of boxes, to be rigged presents a different problem.
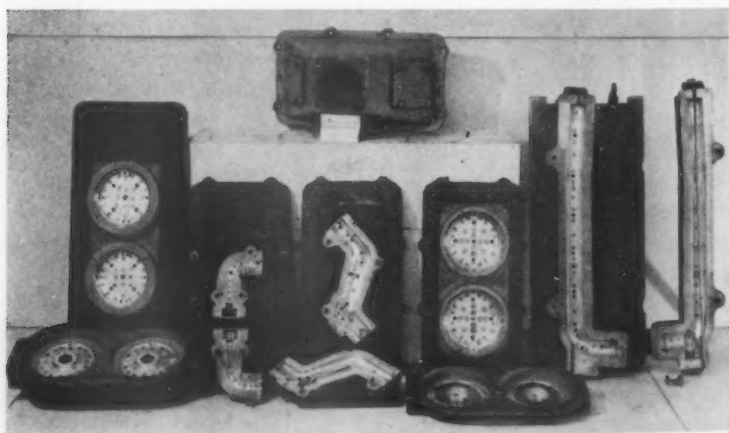
FIG. 4—EQUIPMENT USED TO MAKE FREQUENT CHANGES WITH A MINIMUM OF LOST TIME.
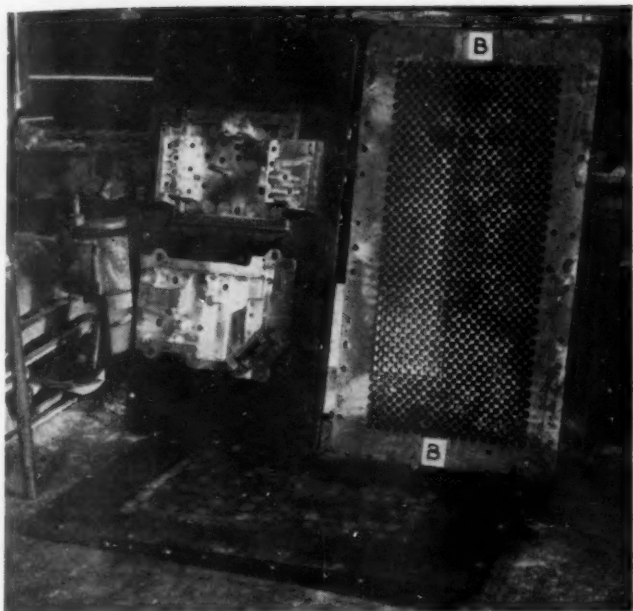
FIG. 5—MASTER BLOW PLATE (B) FOR LARGE CORE BOXES.

CORE BLOWING EQUIPMENT

20.   Equipment that has served for years on some machine or bench, can be, and in many cases has been, rigged successfully for blowing, but the fact still remains that equipment can best be rigged for quality and quantity if thought is given, starting with the blue print, in planning for core blowing. Then the probable location of vents and blow-holes is anticipated, proper handling facilities provided and core box weights controlled.

21.   The writer does not intend to describe in detail the mechanical functions of a core blower, but for the benefit of those who are not familiar with them we present some illustrations. In Fig. 3 there is shown at A the sand reservoir or magazine, as it is commonly referred to in many shops. The plates in these pictures we refer to as "magazine plates." The five plates in this figure and one shown at B in Fig. 5 serve for approximately one thousand different core boxes of all shapes and sizes.

22.   The method used to make frequent changes with a minimum of lost time is illustrated in Fig. 4. The upper halves of 3 core boxes are shown bolted to the magazine plate, and two sets of loose

pieces on the other two. It should be noticed that these core boxes
are almost identical. They are made to be adaptable to either plate.
The machine on which these cores are made depends entirely upon
schedule requirements.

23.   The core box, attached to a $\frac{3}{8}$-in. steel plate, is placed
underneath the rails of the magazine slot, sliding in from either
end. The core box is located in the center and sealed against the
magazine plate by drawing up the bolts. The number of bolts used
depends entirely on the length of the core. The plate to which the
box is attached should overlap the opening in the magazine plate
at least one-in. for the best results.

24.   The sixth blow plate referred to in paragraph 21 as shown
at B, Fig. 5, is always used as a master plate on large core blowers.
In this plate there are approximately 730 holes. One-half of these
are $\frac{5}{8}$-in. and the others $\frac{1}{2}$-in. diameter. It is not difficult to real-
ize that when large core boxes are rigged for blowing machines,
the holes in the box will correspond with some holes in the plate
closely enough to obtain good results, thus eliminating the necessity
of making individual plates. This illustration brings out the fact
that when core boxes are considered too large for a small blower,
and not large enough for a large one, two such core boxes can be
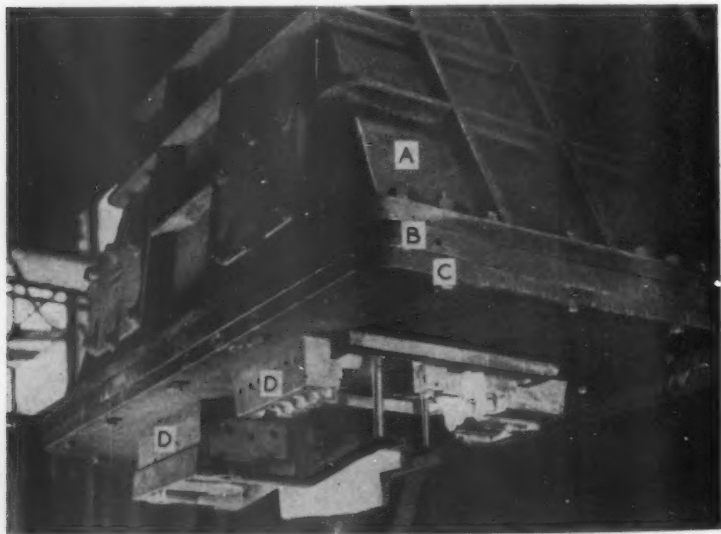
FIG. 6—SHOWING ASSEMBLED BOXES ON CORE BLOWER. (A) MAGAZINE, (B) MAGAZINE
PLATE, (C) BLOW PLATE, (D) UPPER HALVES OF CORE BOXES. THE LOWER HALVES OF
THE CORE BOXES ARE MOUNTED ON MACHINE TABLE DIRECTLY UNDERNEATH UPPER HALVES.
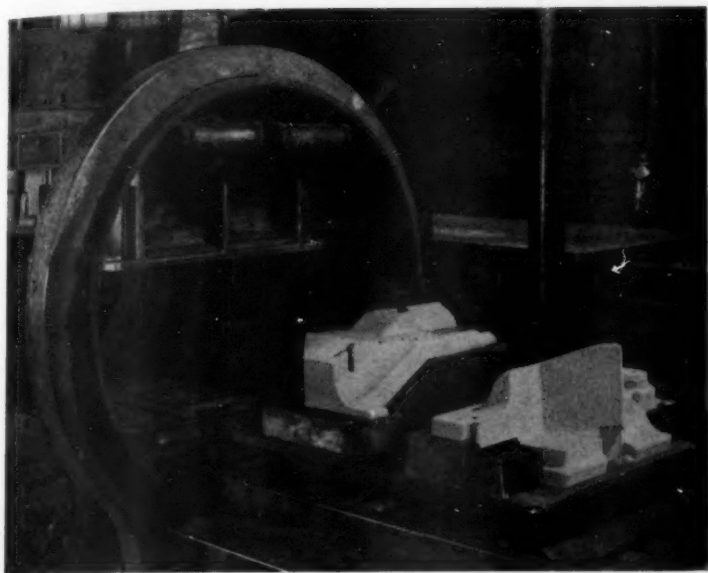
FIG. 7—ROLL-OVER EQUIPMENT SHOWING TWO COMPLETED CORES.

fastened to a single plate and run on a large blower. It is evident that the drawing and replacing of loose pieces and of the upper half of the core box by hand are entirely eliminated.

25. In Fig. 6, the magazine, A, the magazine plate, B, the blow, plate, C, the upper halves of the core boxes, D, and all loose pieces are assembled as one unit. The lower halves of the boxes are located on the machine table directly underneath the upper halves. The air is then released into the clamp diaphragm, which raises the draw table. This, in turn, forces the throat of the magazine against a sealing ring directly under the blow valve. The valve is next released and the blow completed, that is, providing all joints are properly sealed and the core box area does not exceed that of the piston in the draw table.

26. The two completed cores shown in Fig. 7 do not even resemble one another. This two box practice is carried out only when both cores are to be used for one and the same casting. It is not advisable to rig boxes for the purpose of running them together unless production requirements warrant it.

27. A complete sequence of operations on one of the large blowers is shown in Fig. 8. After the core, C, is blown at A, it proceeds along on the conveyor to the roll-over machine, B, where the roll-

over and drawing are completed. The cores are removed to the baking ovens while the boxes return to the blower on the conveyor *D* in the background, where the boxes are blown out, wired and the loose pieces replaced. The operation of blowing these cores is then repeated in proper sequence.

28. Figure 9 is shown to emphasize the uniformity obtainable in cores made on a blower, particularly in relation to sagging. These cores, when assembled, form the main body of a six-cylinder diesel block. Each core is 28-in. long, 13-in. wide, and they vary in height from 3- to 8-in. There is a $\frac{1}{8}$-in. finish on all main bearing faces and $\frac{1}{8}$-in. on each side of the cylinder bores.

29. After baking, the cores are not rubbed to fit, but are placed directly on an assembly gauge and bolted together. Due to the small amount of finish allowed there must be a complete absence of distortion. For example, if each of the 12 cores sagged 1/64-in., the completed assembly unit would be 3/16-in. too narrow, with the result that the outside bore and main bearing would be 1/16-in. under specified requirements. Actually in our practice the distortion in these cores is so slight it is hardly measurable.

30. The examples shown in Fig. 10 were brought together pur-

Fig. 8—Illustrating Sequence of Operations on Continuous System. Core Blown at (A), Roll-Over and Drawing at (B), Cores on Plates (C) Being Conveyed to Baking Racks. Core Boxes Being Returned by Conveyor (D) to Core Blower.
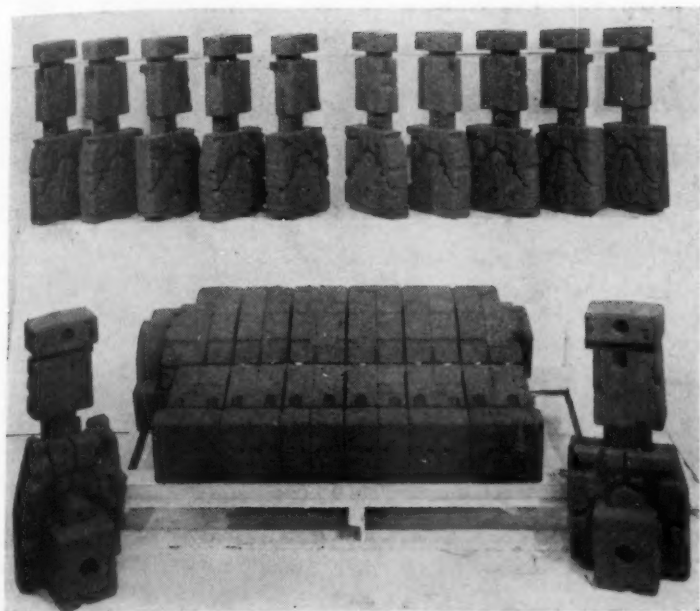
Fig. 9—Individual Cores Made on Blower and Assembly Showing Accuracy of Baked Cores.

posely to emphasize the tremendous amount of time that can be saved when blowing cores of this type. Unfortunately, the size and shape of loose pieces attached to these plates are rather hard to distinguish. The advantages to be gained when blowing cores of this type can readily be seen when considering the amount of ramming that is eliminated on a core 40-in. long and $\frac{1}{2}$-in. thick.

31. The question often asked is, "Why are the blow-holes in this example situated so closely together?" When the sand enters the box these holes serve two purposes. First, they hold the wire intact on stools provided for them as the blow-hole is situated directly above them, and, second, one blow-hole counteracts the sweeping action of the others above it. This example brings out the fact that blow-holes are not always used to merely blow sand into the box. If the blow-hole is not used to hold wire in position when the sand enters, some other means must be employed. When it becomes necessary to use stools for this purpose the impression which is made must be plugged with a patching sand or mudding compound.

32. The cores pictured in Fig. 11 were made in the boxes
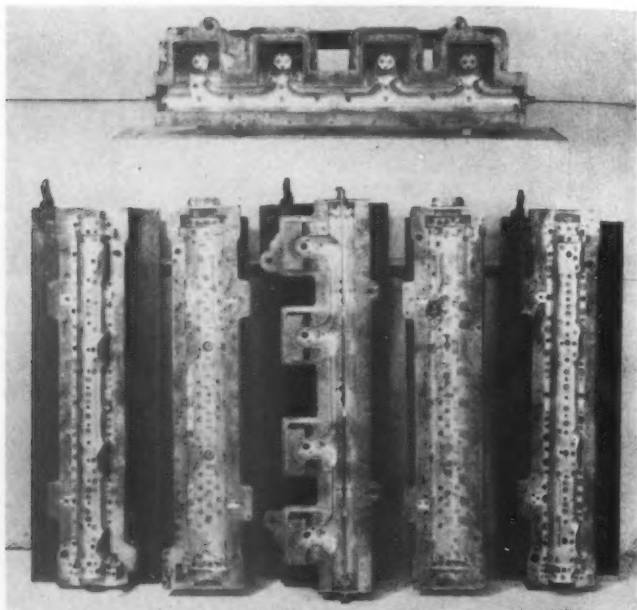
FIG. 10—EXAMPLES OF CORE BOXES FOR BLOWING TO SHOW SAVING IN TIME POSSIBLE BY BLOWING.
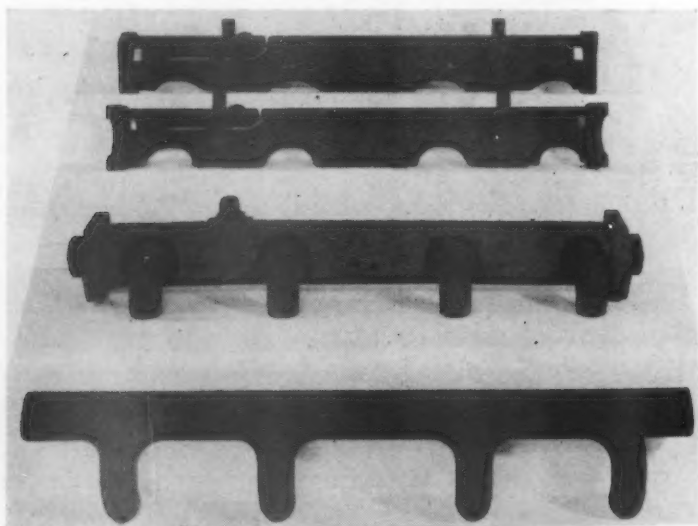
FIG. 11—CORES MADE FROM BOXES OF FIG. 10.

shown in Fig. 10. It is readily seen how intricate these cores are
and the amount of time that would be required to make them on
any other type of machine than a blower. When they are blown,
they come out so close to correct size that no additional work is
required, such as filing, fitting, etc. They are placed in a rubbing
gauge and a 1/16-in. cut taken off the joints before they are de-
livered to the foundry. By the hand method, however, these cores
frequently would require some dressing and fitting in order to
be satisfactory for foundry use.

Small and Intricate Cores

33. Up to this point, we have been concerned in this discussion
with large cores only. At this time we present for those who are
interested in the smaller more intricate cores the group shown in
Fig. 12. This selection will give a general idea of the variety of
small cores that can be successfully blown.

34. *Pin Cores:* All pin cores, with the exception of a very few,
can be blown in a vertical position. Both vertical and horizontal
clamps are used. The blow plate used does not have holes corre-
sponding to individual cores. The length and width of the single
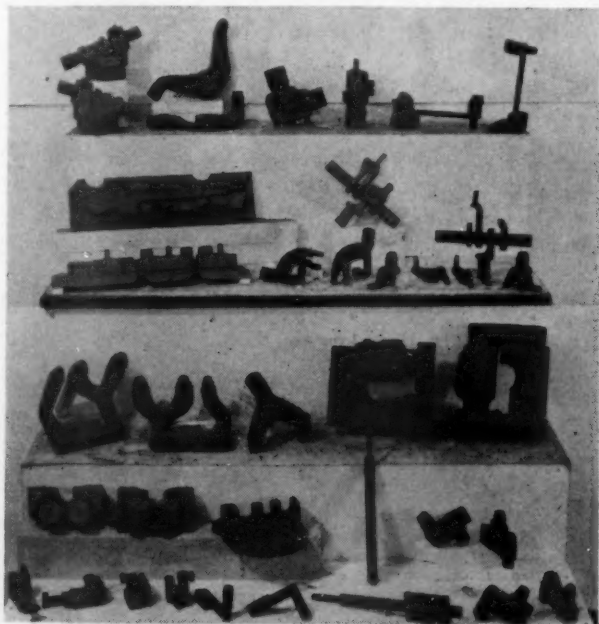
FIG. 12—SMALL INTRICATE CORES MADE BY BLOWING.
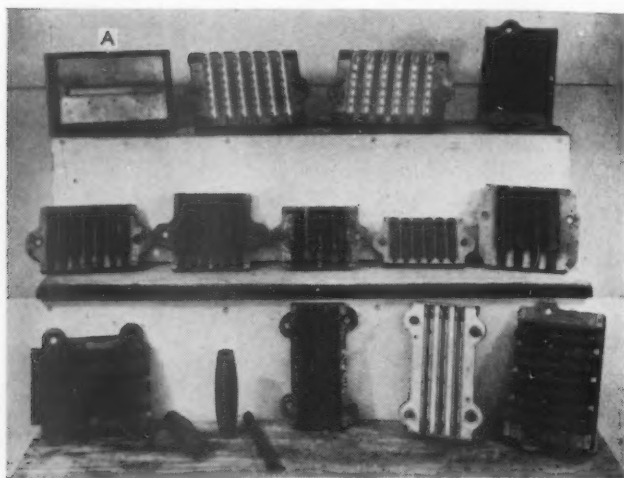
FIG. 13—PIN CORES MADE IN GANG BOXES USING BLOWING METHOD. BLOWING PLATE (A) CAN BE USED ON ANY BOX THAT WILL COVER OPENING IN PLATE. CORES BLOWN IN VERTICAL POSITION.

elongated blow-hole depends on the length of the box and the diameter of the core. The plate shown at A in Fig. 13 can be used on any core box that will cover the opening. The number of cores or their diameter are of no importance.

35. The method of blowing shown in Fig. 14 is identical to blowing the pin cores of Fig. 13. The pin core is blown in a vertical position from one end and the core in Fig. 14 is blown in a horizontal position from both ends. The sand chambers on both ends of the box A are hollow and there are corresponding openings in the plate to which they are attached. Steel bushings are inserted in the chamber with a break-off pad underneath. These pads are more easily distinguished on the core. The driers are constructed to stack and interlock, thus conserving oven space.

Life of Box

36. The approximate life of a core box used on a core blower as compared with one used in hand making seems to be a question most often asked. The degree of skill applied when rigging a core box will naturally determine the maintenance required to keep it in operation. It is advisable for beginners to start with the more simple type of core box, for if an error must be corrected, it is then not a costly one. Additional experience is acquired with the rigging of each box and it is not long before the observant super-

visor is capable of doing a first-class job. Figure 15 is an example of a set of water jacket core boxes for a diesel cylinder head. For about 2 years these boxes were used on air jolt hand roll-over machines. Then they were rigged for the core blowers. These boxes have now been in use about 2 years, and approximately 100,000 pieces have been blown. The condition of the cores and boxes in this example indicate that they will serve the purpose for several more thousand pieces.

ALUMINUM AIRCRAFT CYLINDER HEADS

37. In recent months many foundrymen have been concerned with the production of aluminum air-cooled cylinder heads. Caterpillar Tractor Company's foundry personnel became interested in the possibility of blowing the cores for these castings. A complete set of patterns and core boxes was secured and experiments started.

38. Although we were successful in our efforts, anyone familiar with core blowers can readily see the problem we were undertaking. Numerous experiments were carried out day after day.

Wear on Pattern

39. The first consideration was the minimum amount of wear on the pattern itself, the object being to obtain very much the same results that we would get by either throwing the sand between the fins or jolting the sand between them to the required hardness. A sheet wax model was made, molded to the shape of the pattern, which in turn was used to make an aluminum cast-
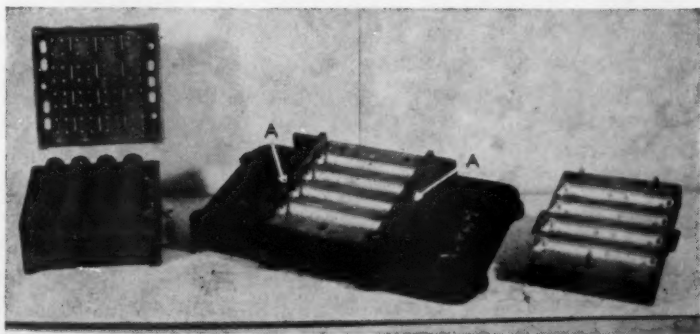
FIG. 14—CORES SIMILAR TO THOSE OF FIG. 13 BUT BLOWN IN HORIZONTAL POSITION. SAND CHAMBERS (A) OF BOX ARE HOLLOW WITH HOLES CORRESPONDING TO HOLES IN PLATES TO WHICH THE BOX IS ATTACHED.
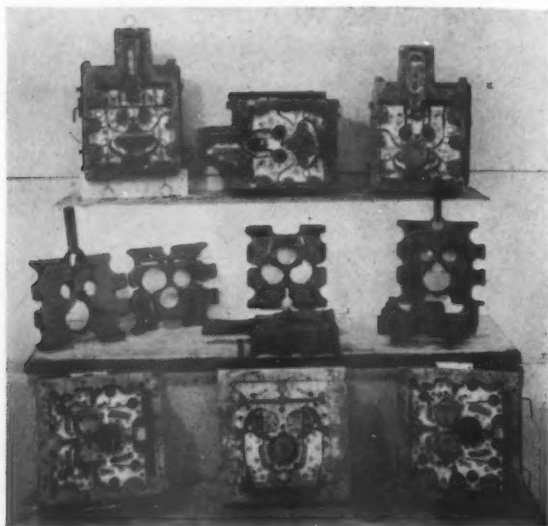
FIG. 15—SET OF WATER JACKET CORES FOR DIESEL CYLINDER HEAD AFTER TWO YEARS' USE.

ing to which we will refer as the jacket. This jacket was fitted to the pattern with a one-in. clearance around the extreme bottom, $\frac{1}{4}$-in. clearance over the entire fin area, and the contact points were held under 0.004-in. to eliminate any possible leakage.

Blow-Holes

40.   The sand is blown through $\frac{1}{8}$-in. elongated blow-holes, cut in the jacket in such a way that all sand entering between the fins would travel in a straight line. This precaution was taken to prevent the sand from striking any of the fins from an angle, as this would produce a cutting action and cause unnecessary wear. A row of vents was installed completely around the pattern underneath the fins.

41.   It was assumed that these vents would hold fast the first sand entering the box, thereby providing a cushion for the remaining sand. As additional sand entered in a straight line down the jacket wall, it was forced between the fins, rather than being blown directly on them. A certain amount of air would have accumulated prior to the time the sand has reached the extreme top of the pattern. A series of vents was installed in the underneath side directly in the center of the jacket to provide an outlet

for the air, allowing the sand to flow with enough force to get a solid ram.

Sand Mix

42. Our next problem was to mix sand which would perform satisfactorily. Several mixtures of sand were tried and an accurate record filed. First, the sand must have considerable flowability, second, it must have green strength of at least one lb. to withstand handling, and, third, the sand must be held at 2.8 per cent moisture content, with a dry permeability of approximately 70. It must break clean, leaving none of the fins adhering to the pattern.

Removal of Cores

43. With the required sand mixture determined, we next considered the removal of the core. The ordinary amount of vibration used in most shops making these heads is not sufficient for blowing practice. We milled through the bottom of the pattern plate and fastened directly to the main body of the pattern a one-in. vibrator. Two $\frac{5}{8}$-in. vibrators were attached, one to each of the rocker arms. The cores of Fig. 16 are illustrative of the results obtained to date.
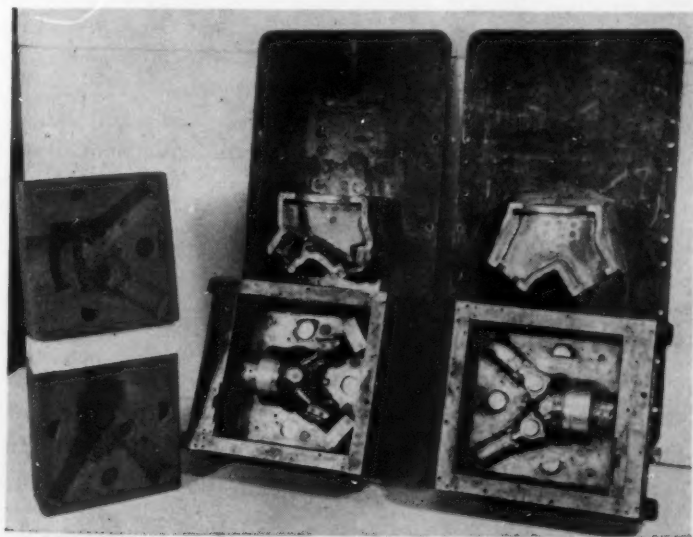
FIG. 16—ALUMINUM AIRCRAFT CYLINDER HEAD CORES MADE BY BLOWING, SHOWING BOXES AND PLATES.

VENT HOLES

44.   When discussing core blowing operations and the rigging of core boxes for them, the question often asked is, "How many vents and what sizes should be used?" Several attempts have been made at setting some standards for this practice. However, none of these, to our knowledge, has been successful to date. The correct sizes and proper locations must be determined by considering the size of the core to be blown, the contour of the box cavity and the kind of sand mixture to be used. The former is determined through experience and the latter depends on the fineness of the sand.

45.   There are several kinds of air vents on the market. In most cases where a high permeability sand is used, a vent with 0.015-in. openings is recommended. Vents with 0.010-in. openings for sands with a lower permeability can be used.

BLOW-HOLES

46.   The number of blow-holes to be used and their sizes also create considerable discussion and numerous differences of opinion. The required number of blow-holes and their sizes depend to some extent on the amount of air pressure available. Lower air pressure requires greater vent and also blow-hole area. Higher air pressure requires less of each. In either case, vents must be used in correct proportions to blow-holes. The shape and size of the core to be made will be the determining factor. For instance, if a bulky core is being made which must dry on a flat plate without a core drier and with close tolerances to be held, the sand used must contain considerable green bond. The speed and force with which a core shall be blown to get the desired results depends on the supervisor's good judgment which he can acquire with experience.

DISCUSSION

Presiding: M. J. GREGORY, Caterpillar Tractor Co., Peoria, Ill.

H. M. LANE[1]: Have you had to go to a pressure above ordinary 100-lb. air, using booster pressures, or do you adhere to the 100-lb. air?

MR. GREGORY: We do not believe we need over 100 lb. of pressure, nor even 90 lb. of pressure. We have seen boosters put on 125 and 135 lb., and we always attribute that to lack of proper holes and lack of vents, and trying to send the sand a long way.

There are cores, for instance, the octopus core which is part of a supercharger, that are blown with only 70 lb. of air.

You will find that a cereal binder will help a great deal, the binder acting as a vehicle for that particular purpose.

JAMES L. HALL[2]: I should like to know more details of the sands that you use, including A.F.A. Grain Fineness No., amount of binder, amount of bentonite; in fact, everything you can give us.

MR. GREGORY: We are all familiar with the different types of sands, coming from the different districts, so one simply has to adapt himself to conditions, just what sand he wants to work with, considering the nature of the job, and whether it is one that requires a more open sand or whether it requires a finer sand.

We have sands anywhere from A.F.A. Grain Fineness No. 50 to 80. Some of it is washed and dry but, on our larger cores, we use a crude sand, using it as it comes and taking into consideration its variations. To this, we add about 5 per cent cereal binder. Our Mr. Shipley is in the audience, and he can check me on this.

F. SHIPLEY[3]: We run a cereal binder from 1 to 50 to 1 to 25.

MR. LANE: How much oil do you use?

MR. SHIPLEY: Jackets run 1 to 60 to 1 to 80, depending upon the size of the jacket, and those heavy cores will run 1 to 50 and 1 to 70.

DR. HALL: How much bentonite do you use? Is there any relation between ratio of bentonite to cereal or bentonite to oil?

MR. SHIPLEY: We do not use any bentonite.

G. C. CLOSE[4]: When the rocker arm boxes for that original body core you worked on, for a cylinder head, were returned to Fairfield, we tried to blow the rocker arms and the sand mixture we were using went through the vents. We were using 10/1000ths vent slats for our fine sand, and I suppose the 15/1000ths vent slats you use explains it. Would you say that the grain size and the type of sand you are using has a lot to do with it; the size of vent slats used?

MR. GREGORY: There is a rocker arm of a cylinder head there, and I assume the sand we are using is, perhaps, a little coarser than yours. You can obtain closer vents if you want them, getting them down to about 8/1000ths.

[1] Grosse Isle, Mich.
[2] American Manganese Steel Div., American Brake Shoe Co., St. Louis, Mo.
[3] Caterpillar Tractor Co., Peoria, Ill.
[4] Aluminum Company of America, Kansas City, Mo.

MR. CLOSE: We are running 10/1000ths regularly. We had standardized on that first, having gone to a little larger one, but the grain size does have quite a bit to do with it.

MR. GREGORY: The grain size, no doubt, has a relation to the screen or the cut vent in the core box. If your sand is too fine, you have to get finer vents. Now, it is a question of whether you need it that fine to achieve the surface you are seeking.

MR. CLOSE: In blowing these boxes, where the blow holes are on the surface of the core, it always leaves a mark.

We ran into a little problem. Where we were putting a drier on a fine point a crush was apt to occur. We have been trying to devise methods to make that sand pull up in the blow plate, rather than leave a lump on the core. Have you found any satisfactory way of handling this problem, possibly some special mechanics, like threading the blow hole, or anything of that nature?

MR. GREGORY: We have tried everything that we could think of, but usually the upkeep of that solution was more undesirable than slicking off that particular blow scar—we call it a scar.

MEMBER: I think it might be well to discuss sand mixtures for the thin-body cores a little more thoroughly. You refer to a sand mixture having 1-lb. green strength and about 2½ per cent moisture. Does that refer to back-up or facing sand?

MR. GREGORY: Your question might apply to that aluminum head, so we will go right to it and tear it all to pieces.

About 95 per cent of the sand used for making the fin body is of 75 fineness. It is an Ottawa district sand. The other 5 per cent is a sand coming from the Sandusky district. That particular sand has approximately 10 per cent clay, whereas the Ottawa district sand, being washed and dry, has no clay in it.

There is 2.5 per cent fly ash in the facing. The ratio of core oil by volume is 40 to 1. That is all there is in the facing sand.

The backing sand is an entire crude Ottawa silica sand with a cereal binder and some oil. It plays no important part to the particular core, other than a backing sand. The ratio of that core oil would be about 1 to 80.

P. F. HABER[5]: I am interested to know more about the cylinder head that is on display. Just how do they support the fins in the core? What sort of wires or nails do they use?

MR. GREGORY: There are about 40 nails in the entire head, roughly speaking, about 20 on each side, primarily to hold the loose fins. There are some fins that run nowhere; they stick out and are not supported on one end. There are about 15 of those on each side, 15 wires placed promiscuously to assist in carrying the molds into the oven. Say 50 wires for the entire head.

The facing sand is vibrated. They vibrate the sand as it is placed over the pattern into a form and then it is jolted about 25 to 30 times. Some of the sand is removed by hand, where they want to put a few nails in, and, after those nails are in, they cover it up with a little fac-

ing and add a backing sand to fill the form and jolt it. Then, they take it off of that particular machine, roll it into a core blower and quickly blow it.

The nails are just wires that are straight, about 40/1000ths. The length varies anywhere from 2½- to 4-in. The operation for making that core hardly takes 3 min. This particular casting simply has been ground around the rocker arm and valve, without grinding in the fins, and has been only sand-blasted.

R. F. LINCOLN[6]: On a job rigged with the upper half of the core box attached rigidly to the blow plate, where you have to take your vents off at the top and possibly out through the blow plates, have you encountered much difficulty in keeping those vents clean? Also, does it present a problem where you have the upper half of the core box attached to the blow plate?

MR. GREGORY: We do not find any greater problem keeping the vents in the cope half clean than in the drag half. We do notice, however, that the moisture will shoot up toward the top side but, as far as difficulty, I say no. With a slotted vent, one has to clean those boxes immediately after removal from the machine, because the sand will adhere; with a screen vent, you can put them in caustic soda and boil them clean.

If you keep the machine running all of the time, you can go all day long without cleaning because it does not have a chance to dry out. It is when the slots get dry that you experience difficulty.

MEMBER: How do you clean the vents when they become plugged? We have tried to blow them out but, of course, you cannot always do so. Therefore, we have used a kind of safety razor edge with a little piece of brass soldered to it.

MR. GREGORY: You have exactly the idea. Just take a sharp instrument—a knife will do—and dig them out. Where you are not allowed to have a depression by some other form of vent, you are compelled to go to that slotted vent in order to get a straight surface.

MEMBER: Do you use any sand mixes for core blowers which do not contain oil?

MR: GREGORY: No, all of our sand mixes contain oil.

MEMBER: Do you think it possible to blow a core with a sand mix that does not contain oil?

MR. GREGORY: I think I would answer your question in the affirmative. Naturally, we would require some kind of a binder to bind our sand. If we were depending upon a cereal binder to do it, there would be a chance that we would need so much that it would be difficult to blow it. I believe water would act as a moistener instead of oil.

MEMBER: You think, then, that you should run a higher moisture content without oil?

MR. GREGORY: I think you would have to use the equivalent of moisture, possibly a little higher. We know that, in the larger cores, we have larger blow holes and we can run higher moisture. In other words, if

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[6] Osborne Manufacturing Co., Cleveland, Ohio.

we had a large core and used a low moisture, we would have difficulty blowing that core.

Referring again to that supercharger core, the moisture in that is higher by at least 2 or 2½ per cent than it is in some of the smaller jacket cores.

MEMBER: I should like to know a little more about the iron oxide. You mention putting it in a core to prevent "rat tails." If it prevents "rat tails" in a core, how will it work in a facing? Would common iron ore be all right?

MR. GREGORY: Of course, we know that iron oxide might be a form of iron ore. The only difference is that the oxide is ground and screened and fairly free of foreign material, such as wood, etc. I do not think you could use straight iron ore. Iron oxide is used in the core to more or less take up what we call hot strength. We know there is expansion in the sand at various temperatures, and we have found with experience that 2 per cent seems to be about the limit of doing any good.

Some people say it can be done with silica flour. We know that silica flour has its good points as well as its bad points, but we find about 2 per cent of the ground and well-treated iron oxide is good. It is the cheapest form of iron oxide.

H. BAILEY[7]: What is the green strength of sand to be used in a core blower?

MR. GREGORY: We do not run over 3 lb.

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[7] American Steel Foundry, Granite City, Ill.

Heat Treatment of Medium Carbon Cast Steel in Moderately Heavy Sections†

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Abstract

Six 10- x 10- x 20-in. castings and 6 each of 2 types of test coupons were cast of approximately 0.25 per cent carbon steel. These castings and coupons were subjected to various heat treatments designed to shorten the annealing treatment sometimes applied to moderately heavy steel castings of this approximate chemical composition. Savings in heat treating time were achieved by accelerating heating rates, by reducing soaking periods, and by utilizing air-cooling and quenching. Physical properties were improved rather than harmed. Further work is in progress.

Statement of Problem

1. In view of shortages in heat treating facilities and the necessity for increased production schedules, the Naval Research Laboratory was authorized by the Navy Department to undertake an investigation to shorten the heat treating time and/or improve the physical properties of moderately heavy steel castings used in Naval Service.

2. Work beyond that reported here is in progress, and, although complete results will not be available until later, the test results to date are thought to be of sufficient value to merit publication.

General Considerations

3. Heat treatment may improve the physical properties of steel castings in the following four ways:
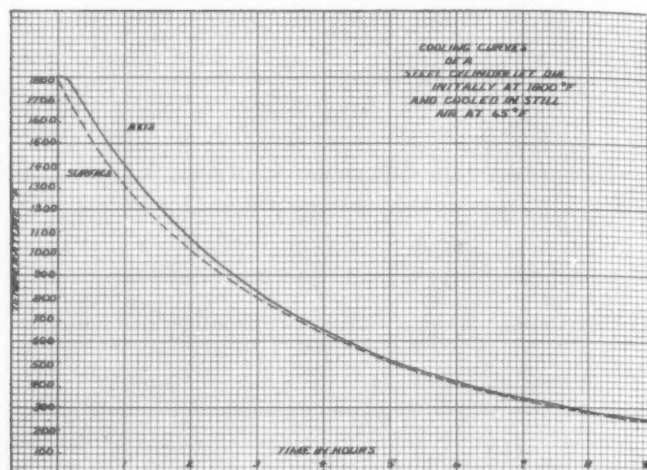
FIG. 1—COOLING CURVES OF A STEEL CYLINDER ONE FT. IN DIAMETER. INITIALLY AT
1800°F. COOLED IN STILL AIR AT 65°F.

Homogenization

4. All alloys which complete their solidification as solid solution phases are characterized by dendritic segregation and medium carbon steel is no exception. Diffusion acts to modify this segregation when steel is heated into its austenitic range and since the rate of diffusion is a function of temperature, high homogenizing temperatures may be employed to advantage, particularly when slow moving elements are present. Manganese, chromium and other carbide-forming elements diffuse slowly even at 1800°F. They attract carbon during transformation upon cooling, especially if the cooling rate is slow, thus giving massive pearlite and ferrite areas in the resulting microstructure. Phosphorus is also very difficult to move. Since phosphorus and carbon occupy the same region only with great difficulty, phosphorus-rich areas will appear as massive ferrite areas in the final microstructure. The degree of chemical segregation is intensified by a slow solidification rate which is, of course, dependent upon section size.

Grain Refinement

5. When steel is taken through its critical temperature range, grain refinement usually results. An originally coarse microstructure makes refinement difficult, and, if homogenization has not been thorough, refinement is apt to be partial and non-uniform.

Sims[1*], in his work on grain refinement of cast steel, illustrates this clearly in the case of improper manganese distribution. Even assuming that complete and uniform refinement is accomplished, heating progressively above the critical temperature range promotes grain growth. This grain-growth tendency can be lessened, of course, by deoxidation practice in melting. Even so, prolonged or excessively high heating during the final sojourn above the critical temperature range will likely produce coarse microconstituents.

Carbide Dispersion

6. At any given level of hardness, the greater the degree of carbide dispersion, the better will be the combination of strength and ductility. Thus, the properties of coarse pearlite are not as good as those of finer pearlite formed by faster cooling and this structure, in turn, is not as good as the very fine dispersion of carbide produced by quenching and tempering. The foregoing statements hold for wrought steels and should hold for cast steels.

Stress Relief

7. When rates of cooling in the mold or during heat treatment differ greatly in various members of a casting or even in the

FIG. 2—TEST CASTING BEFORE REMOVAL OF GATE AND RISER.

* Superior numbers 1 to 10, inclusive, refer to bibliography at the end of the paper.
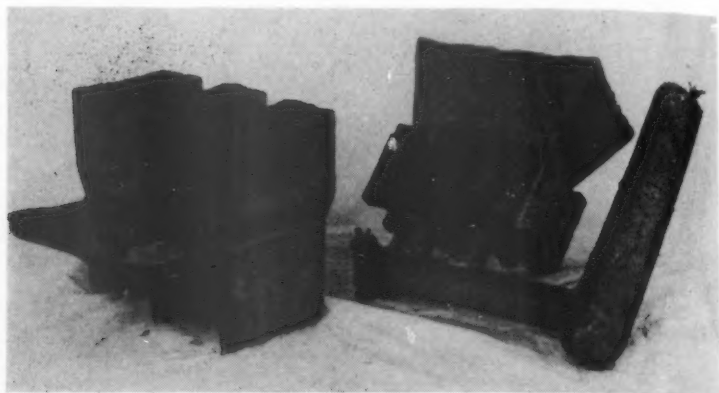
FIG. 3—TEST COUPONS. TYPE A (LEFT) AND TYPE B (RIGHT).

same member, residual stresses of considerable magnitude are set
up. Although retained stresses may be employed to advantage in
certain symmetrical sections, they should be avoided in most cast-
ings. The mechanism of stress relief involves heating the metal to
a temperature where plastic flow will occur, followed by cooling at
a rate which will prevent harmful stresses from being re-estab-
lished.

HEATING AND COOLING RATES

8. The very slow heating and cooling rates often prescribed in
the annealing of heavy castings were probably employed because
of the early belief that the surface of a large casting rapidly
reached the temperature of the furnace while the center remained
cold, and that, in air cooling from a high temperature, the surface
reached the temperature of the atmosphere much more quickly
than did the center. If this were true, cracking or tearing would
result from the high stresses set up in large sections due to extreme
temperature gradients, and slow heating and cooling would be
necessary to prevent this condition. The fact is, however, that mild
steel is a relatively good heat conductor, and there is surprisingly
little temperature lag between the center and surface of a casting
on rapid heating and cooling, so no trouble should arise from
cracking due to thermal stresses in fairly uniform sections of
mild steel.

9. At the Naval Research Laboratory a 4-in. diameter cast
steel cylinder, into which thermocouples were inserted to various

depths, was placed in a furnace at 1250°F. and the temperature recorded at intervals. It was found that the whole casting reached the furnace temperature at about the same time and at no time was there a temperature difference between the surface and the center of the casting of over 50°F. When the casting reached the furnace temperature it was removed from the furnace and allowed to cool in air. Again it was found that the casting cooled almost uniformly throughout with about the same temperature difference prevailing between the center and surface on cooling as on heating.

10. For larger sections cooled from higher temperatures there is still only a comparatively small difference. Figure 1 shows the cooling curves for the surface and axis of a 12-in. diameter cylinder cooled in air at 65°F. from a temperature of 1800°F. These curves are plotted from heat transfer formulae[2]. At no time is the temperature difference between the axis and surface over 100°F. With temperature gradients of this small order existing in a casting, especially a casting made of mild steel and of fairly uniform section, there is very little tendency toward cracking.

ANNEALING

11. Long annealing promotes grain growth instead of grain refinement, and slow cooling through the critical zone causes the formation of large pearlite and ferrite areas, a condition which is not conducive to toughness. The long annealing time produces some homogeneity in the casting, but this could be more quickly accomplished by using a higher temperature. Sims[1] has pointed out that for every 100°F. increase in the annealing temperature
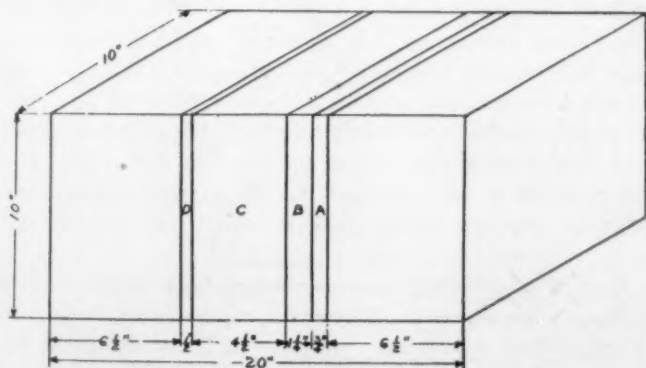
FIG. 4—LINE SKETCH OF TEST CASTING SHOWING POSITIONS OF SECTIONS FROM WHICH TEST SPECIMENS WERE REMOVED
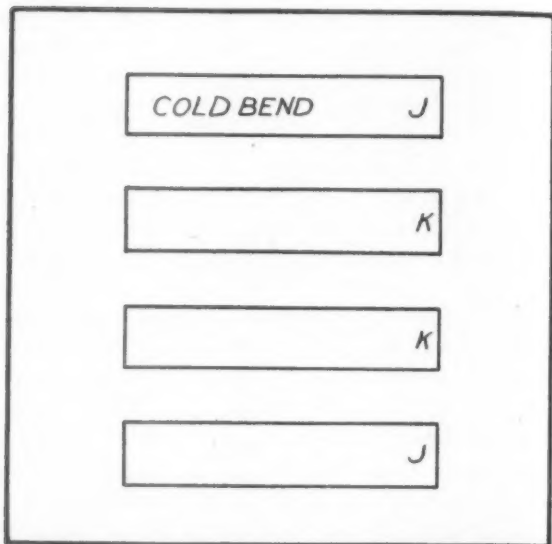
FIG. 5—POSITION OF COLD BEND SAMPLES REMOVED FROM ¾-IN. SECTION A SHOWN IN FIG. 4.

above 1650°F., the time at temperature could be reduced by one-half to obtain the same effect. He based this conclusion on the commonly accepted belief that one hour per in. of the largest section of the casting at 1650°F. produced satisfactory homogeneity.

SOAKING, NORMALIZING AND TEMPERING

12. Soaking at 2000°F. for 2 hours per in. of cross section, followed by normalizing from just above the critical to refine the grain and then tempering was indicated by Merton's[3] work as being the only way by which the properties of cast steel in massive sections could be made to approach the properties of test coupons. He was of the opinion that the production of high quality castings of large cross section was aided by keeping the silicon content low and pouring as cold as possible. He advocated a soaking pit treatment for castings similar to that which rolling mills use for ingots.

13. Lorenz[4] found that, in some cases, a high temperature soaking treatment improved the physical properties of large sections and in other cases it did not. Because of his inconsistent results he concluded that design, gating and feeding influenced physical properties as much as heat treatment.

14. The effect of mass on sections as large as 8- x 8-in. was studied by Briggs and Gezelius[5]. They found that strength and ductility at the center of cast sections decreased as the section size increased. They used as their standard heat treatment an anneal of 1650°F. for one hour per in. of cross section. They concluded that the decrease in mechanical properties at the center of large sections was caused by microstructure, carbon segregation and a decrease in density. In contradiction to Merton, they found that pouring temperature had no influence on the physical properties and that silicon segregation was not of sufficient magnitude to have much effect. Quite possibly, aluminum segregation might influence ductility in different portions of a heavy casting. It was pointed out in the discussion of Briggs' and Gezelius' paper that 0.03 to 0.05 per cent of aluminum would cause a very serious drop in the ductility of acid made steel, especially in large sections. This was explained by Sims and Dahle[6] on the basis of inclusion types formed in carbon steels. Without an aluminum addition, globular non-metallics were formed which had no adverse effect upon ductility. With aluminum additions to a maximum of 0.05 per cent, depending upon the sulphur content, intergranular inclusions of the stringer type were formed and caused a serious drop in ductility. Aluminum additions beyond 0.05 per cent caused the
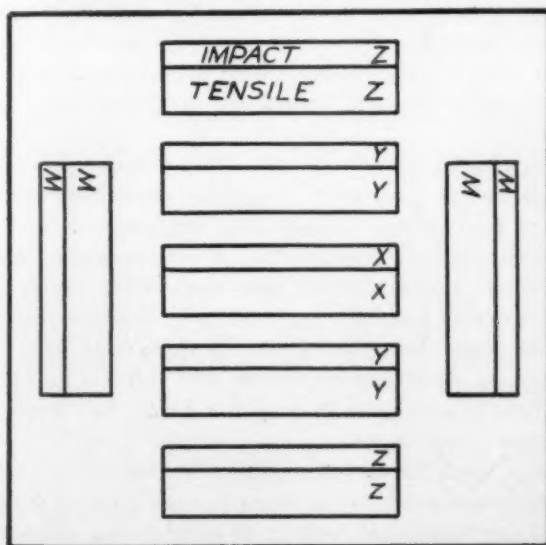
Fig. 6—Position of Transverse, Tensile and Impact Specimens Removed from 1¼-In. Section B Shown in Fig. 4.
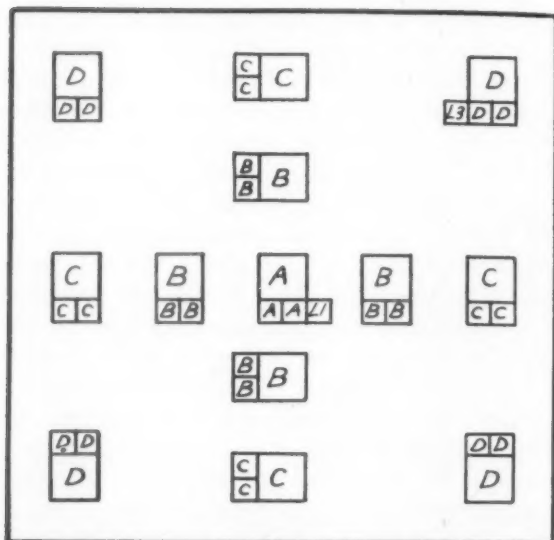
FIG. 7—POSITION OF LONGITUDINAL, TENSILE AND IMPACT SPECIMENS REMOVED FROM 4½-IN. SECTION C SHOWN IN FIG. 4.

non-metallics to again appear in a globular form with a corresponding re-occurrence of good ductility.

15. Hall[7] concluded that heating carbon steel castings to 1830°F. was no more effective in breaking up cast structure than heating to 1650°F. He recommended quenching and drawing for securing superior physical properties in carbon steel castings as early as 1909.

16. The advantages of cooling rapidly through the critical temperature range are reported frequently in technical literature. Armstrong[8] recommends normalizing in preference to single or double annealing for carbon steels. Knox[9] presents data to show that quenching and tempering give remarkably better properties than annealing of medium carbon steel castings. Mitchell[10], in commenting upon the possibilities of quenching and tempering medium carbon steel castings, states that there is no limitation as to size or type of casting to be quenched except as the size is limited by the furnace equipment.

17. Many metallurgists have observed that the values obtained from test coupons must not be taken as indicative of the properties of the casting itself, but merely as a basis for estimating those properties when considered in conjunction with section size, heat treatment, and metal density. The test coupon should be used

only as an indication of the quality of the metal from which it is made.

PROCEDURE

18. Six castings 10-in. square and 20-in. long were made at the Washington Navy Yard from aluminum killed basic electric steel. One of these castings, with gate and riser intact, is shown in Fig. 2. Twelve test coupons, 6 each of the types shown in Fig. 3, were also cast. It is to be noted that the investigation was carried out in 2 parts. Hence, 3 of the large castings and 3 of each of the coupons were cast from one heat, whereas the other 3 large castings and corresponding coupons were cast from another heat. Chemical composition of the two heats is given in Table 9. Some of the tests made upon the first set of castings (heat R) were chiefly of academic interest and were omitted from the second set (heat S), but all data are included.

19. Each of the large castings, accompanied by one of each type of test coupon, was given a different heat treatment, except that a long anneal was used for one set in each lot as a control treatment. The heat treatments are shown in Table 1.

Table 1

HEAT TREATMENTS

Heat R

Treatment No. 1[11],
  1800°F., 6 hours, AC to 950°F.
  1650°F., 3 hours, AC to 900°F.
  1300°F., 3 hours, FC to 950°F.,
    AC
    Total time involved[12]—36
      hours

Treatment No. 2,
  1700°F., 4½ hours, AC to 900°F.
  1300°F., 3   hours, FC to
    720°F., AC
    Total time involved—32 hours

Treatment No. 3[13],
  1600°F., 25 hours, FC to 865°F.,
  (Heating time, 15 hours; cooling
    time, 24 hours)
    Total time involved—64 hours

Heat S

Treatment No. 4,
  1800°F., 5 hours, AC to 750°F.
  1650°F., 2 hours, WQ
  1230°F., 4 hours, AC
    Total time involved—22 hours

Treatment No. 5,
  1800°F., 2 hours, AC to 750°F.
  1230°F., 4 hours, AC
    Total time involved—13 hours

Treatment No. 6,
  Same as No. 3

NOTE: AC = Air Cooled; FC = Furnace Cooled; WQ = Water Quenched.
[11] No restrictions were imposed upon heating rates except for treatments No. 3 and 6. Rates up to 400°F. per hour were employed.
[12] Time includes that of heating, holding, cooling and manipulation.
[13] Specified for stockless anchors weighing over 10,000 lb.
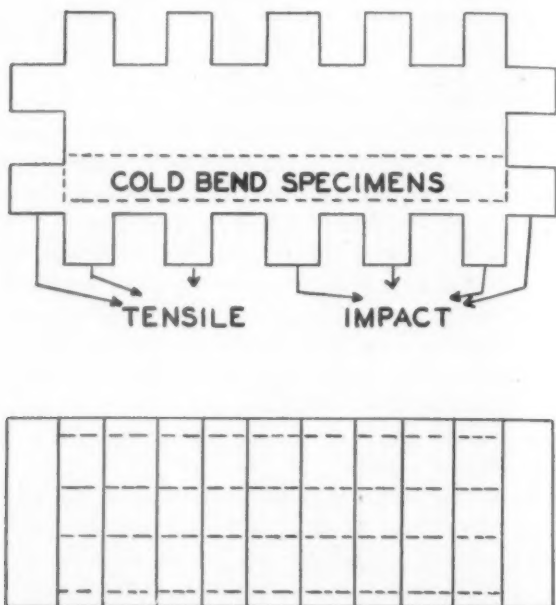
FIG. 8—LOCATION OF TEST SAMPLES REMOVED FROM COUPON B.

20. After heat treatment was completed, the 10- x 10- x 20-in. castings from heat R were sawed into 4 sections, A, B, C, and D, as shown in Fig. 4. Four cold bend specimens were sawed from section A, as shown in Fig. 5, two specimens from near the center and two from near the surface of the casting. The $1\frac{1}{4}$-in. section B was cut into tensile and impact blanks as shown in Fig. 6. The $4\frac{1}{2}$-in. section C of Fig. 7 was also cut into blanks for tensile and impact specimens so that the properties in a longitudinal direction could be determined. The $\frac{1}{2}$-in. section D was machined and ground. Fifteen Brinell impressions were made on each section from one
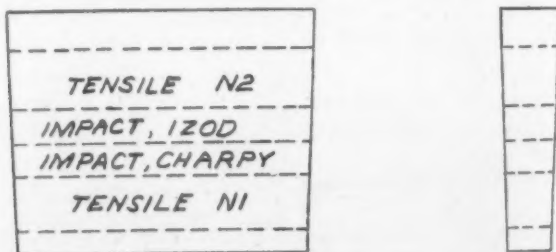
FIG. 9—LOCATION OF TEST SAMPLES REMOVED FROM COUPON A.

corner diagonally across to the opposite corner. After hardness data were taken the ½-in. sections were etched in boiling 50 per cent hydrochloric acid for 30 min. Tensile, cold-bend, and impact specimens were obtained from the coupons as shown in Figs. 8 and 9. The 10- x 10- x 20-in. castings from heat S were sampled in a similar manner except that transverse tests were omitted. Also, the number of test specimens from the 4½-in. section C was reduced and the method of sampling was changed slightly. Tensile and impact blanks were taken from 3 of the corners, positions D of Fig. 7. Other test blanks were omitted except for tensile and impact blanks grouped closely at position A of the same figure.

21. All tensile specimens used were 0.505-in. diameter shoulder grip specimens with a 2-in. gage length and were broken under a rate of loading of 10,000 lb. per min. The arrest of the dial indica-
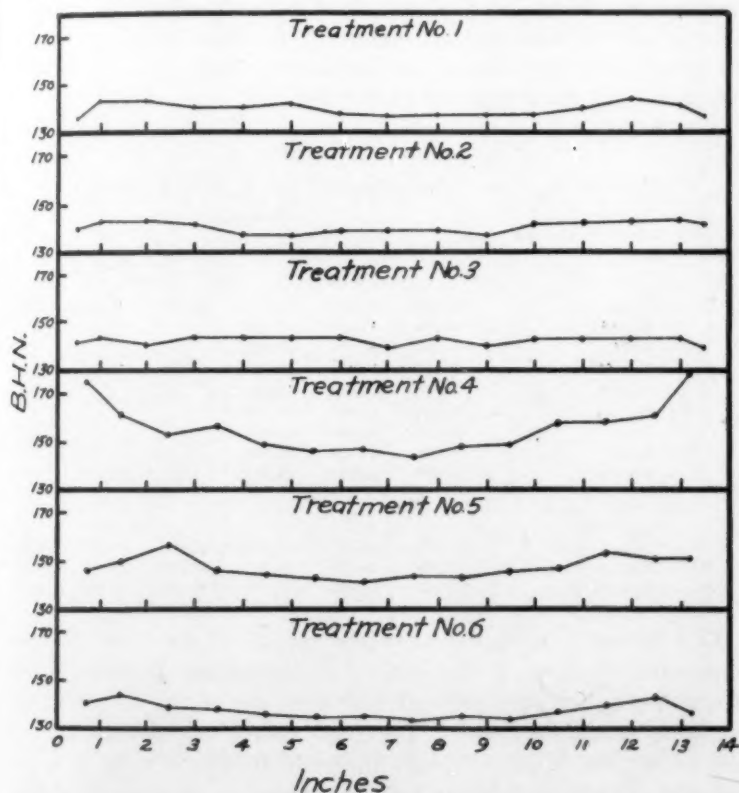
Fig. 10—Brinell Hardness Across Diagonal of 10- x 10- x 20-In. Casting for All Heat Treatments.

tor was used to obtain the yield point. The Izod specimens
were the conventional V-notch type, but had only 2 notches in-
stead of the usual 3. For specimens removed from more than one
similar position in the test casting, which was the case in all but
the center specimens from heat R, the results were averaged.
Charpy keyhole impact tests were made at 70°F. and 35°F. up-
on the original set of castings but the range of testing tempera-
tures was extended for the second set. A sufficient number of
blanks were removed from the center and near the surface of the
10- x 10- x 20-in. blocks, and from the type B coupons to obtain
check results for each location at 70°, 32°, 0°, -20°, and -40°F.

DISCUSSION OF DATA

22. The hardness values are plotted as curves in Fig. 10. All
heat treatments, except No. 4, resulted in good uniformity of hard-
ness across the section. The range of hardness obtained by the
different heat treatments was sufficiently narrow to allow easy
comparison of other physical properties.

Table 2

EFFECT OF HEAT TREATMENTS UPON YIELD POINT

Location of Sample in 10x10x20-In. Casting		1	2	3	4	5	6
			Heat R. lb. per sq. in.			Heat S. lb. per sq. in.	
L*	A— Center	45,000	43,750	40,500	50,650	41,900	39,650
	B— Midway	46,425	43,750	41,400			
	C— Edge	48,700	45,700	43,300			
	D— Corners	49,000	45,900	43,800	58,300	47,600	45,200
T**	X— Center	46,600	44,000	40,550			
	Y— Midway	47,250	44,000	41,700			
	Z— Edge	48,750	46,000	43,000			
	W— Edge	49,000	45,800	42,500			
	Average	47,590	44,860	42,090	54,475	44,750	42,425
Location of Sample in Coupons							
N2—	Top, Coupon A	51,700	48,850	44,500	63,000	48,500	44,000
N1—	Bottom, Coupon A	52,250	48,850	46,000	62,250	48,750	44,250
R —	Coupon B	50,500	48,850	47,500	71,100	48,250	44,500

23. Tables 2 through 8 show the results of the other physical
properties obtained. From Table 3 it can be seen that the tensile
strengths vary within limits of 10,000 lb. per sq. in. following the
hardness variations closely. The yield values recorded in Table 2
also follow the trend, although the yield to tensile ratio is in the
order of 10 per cent better for the homogenizing, quenching and

* Longitudinal.
** Transverse.

tempering treatment than for the long annealing treatment. The normalizing treatments gave intermediate yield ratio values with slight improvement noted for double normalizing over single normalizing. Values obtained from the coupons are slightly higher than those obtained from the 10- x 10- x 20-in. castings. However, the differences from treatment to treatment vary in the same direction and in approximately the same order of magnitude.

24. Tables 4, 5, and 6 show data for the static ductility

Table 3

Effect of Heat Treatments upon Tensile Strength

Location of Sample in 10x10x20-In. Casting		1	2	3	4	5	6
			Heat R, lb. per sq. in.			Heat S, lb. per sq. in.	
L*	A— Center	70,000	67,000	68,000	77,250	72,500	72,500
	B— Midway	71,000	71,250	71,350			
	C— Edge	75,400	75,500	76,000			
	D— Corners	76,000	76,000	78,650	84,500	80,250	79,350
T**	X— Center	73,250	71,500	70,750			
	Y— Midway	73,000	71,000	72,250			
	Z— Edge	76,500	75,000	75,000			
	W— Edge	72,500	76,500	75,000			
	Average	73,460	72,970	73,375	80,875	76,375	75,925
Location of Sample in Coupons							
	N2— Top, Coupon A	75,700	76,250	77,500	88,000	79,250	78,000
	N1— Bottom, Coupon A	76,500	76,250	77,500	87,000	78,750	79,000
	R — Coupon B	76,000	75,500	77,500	93,875	79,750	79,375

Table 4

Effect of Heat Treatments upon Elongation in 2-In.

Location of Sample in 10x10x20-In. Casting		1	2	3	4	5	6
			Heat R, per cent			Heat S, per cent	
L*	A— Center	16.4	9.4	10.9	13.0	15.0	19.0
	B— Midway	15.9	13.3	15.3			
	C— Edge	19.0	19.3	20.3			
	D— Corners	26.8	25.8	26.5	26.0	23.0	20.0
T**	X— Center	15.6	14.1	14.8			
	Y— Midway	17.6	8.6	16.0			
	Z— Edge	17.2	17.2	15.0			
	W— Edge	17.5	14.5	15.6			
	Average	18.3	15.3	16.8	19.5	18.0	19.5
Location of Sample in Coupons							
	N2— Top, Coupon A	25.8	22.7	25.0	23.4	23.4	24.2
	N1— Bottom, Coupon A	29.7	29.7	28.9	28.9	28.9	25.0
	R — Coupon B	32.4	32.0	28.9	28.0	30.0	27.0

* Longitudinal.
** Transverse.

Table 5

EFFECT OF HEAT TREATMENTS UPON REDUCTION OF AREA

Location of Sample in 10x10x20-In. Casting		1	2	3	4	5	6
			Heat R, per cent			Heat S, per cent	
L*	A— Center	18.1	14.5	18.1	21.0	20.0	24.0
	B— Midway	22.0	24.2	17.8			
	C— Edge	23.4	18.0	25.9			
	D— Corners	25.8	37.0	38.5	47.0	31.0	31.9
T**	X— Center	15.9	18.8	18.1			
	Y— Midway	23.0	19.5	21.8			
	Z— Edge	26.3	20.6	20.4			
	W— Edge	21.0	19.0	19.6			
	Average	23.2	21.5	22.5	34.0	25.5	27.5
Location of Sample in Coupons							
	N2— Top, Coupon A	35.0	32.5	25.1	40.0	31.0	33.0
	N1— Bottom, Coupon A	51.7	54.9	41.3	57.0	48.0	39.0
	R — Coupon B	57.4	58.2	45.2	63.0	55.0	44.0

* Longitudinal.
** Transverse.

Table 6

EFFECT OF HEAT TREATMENTS UPON COLD-BEND PROPERTIES*

Location of Sample in 10x10x20-In. Casting		1	2	3	4	5	6
			Heat R			Heat S	
J— Surface	Max. Load	7,600	7,600	7.600	8,300	7,550	7,800
	Angle of Fracture	95	75	70	84	66	70
K— Center	Max. Load	7,400	7,100	7,400	7,750	7,350	7,400
	Angle of Fracture	95	65	75	71	63	68
Average	Max. Load	7,500	7,350	7,500	8,025	7,450	7,600
	Angle of Fracture	95	70	73	77.5	64.5	69
R— Coupon	Max. Load	7,900	8,000	8,000	8,600	8,000	8,100
	Angle of Fracture**	135	135	135	135	135	135

* Load reported in pounds. Angles reported in degrees.
** Maximum angle of bend; no fracture occurred.

results. Elongation values show no definite trends. A low value for at least one position in each test casting appears for all treatments except No. 6. In coupon size, there is little difference resulting from the various treatments. Inconsistency is also in evidence for reduction of area data, although treatment No. 4 appears to be best of all. This is also reflected in the test coupons. From the cold-bend test, it is obvious that treatment No. 1 is distinctly better than all other treatments, except possibly No. 4 in which the angle at fracture was somewhat lower due to higher hardness of the material, 156 Brinell as compared to 140 Brinell. A comparison of the maximum load values further clarifies the point. The effect of mass on physical properties is most pronounced on the ductility.

In Tables 2 through 7 it can be seen that for each value tested the test coupon has better properties than does the block with which it was heat treated, but this difference is most apparent in the tables showing elongation, reduction in area, and cold-bend results. Block 1, for example, has an average elongation of 18.3 per cent, while specimens obtained from coupon B have an elongation of 32.4 per cent or almost twice as much. The difference in reduction of area values is still greater, with block 1 having an average of 23.2 per cent, while the reduction of area of specimens from coupon B is 57.4 per cent. The cold-bend specimens taken from coupon B did not fracture even after being bent through 135 degrees while specimens sectioned from the large test blocks failed to even approach the 120 degrees required in specifications before fracture. This is another proof that properties of specimens removed from coupons should not be taken as other than relative to the properties of the castings poured from the same metal.

Table 7

EFFECT OF HEAT TREATMENT UPON IZOD IMPACT RESISTANCE

Location of Sample in 10x10x20-In. Casting		Treatment No.					
		1	2	3	4	5	6
		Heat R, ft.-lb. at 70°F.			Heat S, ft.-lb. at 70°F.		
L*	A— Center	31.5	22.0	32.0	51.0	40.0	37.0
	B— Midway	34.0	24.0	34.0			
	C— Edge	30.0	20.0	31.0			
	D— Corners	30.0	20.5	31.0	64.0	32.0	27.0
T**	X— Center	33.0	22.0	36.5			
	Y— Midway	32.0	23.0	34.0			
	Z— Edge	27.0	20.5	27.0			
	W— Edge	26.5	21.5	29.5			
	Average	30.5	21.8	31.9	57.5	36.0	32.0
Location of Sample in Coupons							
	N— Coupon A	34.0	25.5	34.0	81.0	35.0	32.0
	R— Coupon B	35.0	27.0	37.5	79.0	33.0	30.0

25. The greatest differences among the heat treatments were shown by the impact resistance (Tables 7 and 8 and Fig. 19). The full significance is not clear as notched-bar impact tests have not been applied extensively to cast steels, but the results indicate that treatment No. 4 is definitely superior to all other treatments. This effect is intensified in the test coupons. An interesting but apparently anomalous behavior is that, with all treatments but No. 4, the central portions of the 10- x 10- x 20-in. blocks exhibit very

* Longitudinal.
** Transverse.

nearly equal or better impact resistance than those portions near the exterior.

Table 8

EFFECT OF HEAT TREATMENTS UPON CHARPY (KEYHOLE) IMPACT RESISTANCE AT INDICATED TEMPERATURES

Location of Sample in 10x10x20-In. Casting	Test Temp.	Treatment No.					
		1	2	3	4	5	6
	°F.	Heat R, ft.-lb.				Heat S, ft.-lb.	
Center Block	70	22	19	20	32.5	27	24
	32	19*	16*	18.5*	27	20	20
	0				29	22	16
	−20				19.5	12	16
	−40				16	11.5	9.5
Surface Block	70	20	16.5	16	37	22	20
	32	18*	14*	14.5*	28	17	18
	0				29.5	18	13
	−20				25.5	13	13
	−40				14.5	16	6
Coupon B	70	25	19	21	46	27	20
	32				36	22	19
	0				30	20.5	16
	−20				25.5	18	13
	−40				24.5	11	10.5

Table 9

CHEMICAL COMPOSITION OF THE STEELS USED

	Per Cent	
	Heat R	Heat S
Carbon	0.24	0.27
Manganese	0.66	0.63
Silicon	0.44	0.43
Sulphur	0.016	0.012
Phosphorus	0.031	0.030
Copper	0.21	0.17
Nickel	0.75	0.73
Chromium	0.22	0.19

26. Photomicrographs from the corner and from the center of each 10- x 10- x 20-in. casting are reproduced in Figs. 11 through 16. In all cases the appearance of the center is very much the same as that of the corner. Treatment No. 4 shows the greatest difference with more distinct separation of ferrite at the center than at the corner. Comparing treatments No. 1 and No. 4, quenching has caused much more uniform distribution of pearlitic areas than has air cooling. Comparing these treatments with the other 4 treat-

* Testing temperature, 35°F.

Fig. 11—Photomicrographs of (A) Center and (B) Corner of 10- x 10- x 20-In. Casting Given Treatment No. 1. Magnification x100.

ments, the benefits of a second heating and cooling through the critical temperature are apparent. The effect of the austenitizing temperature upon final grain size can be observed by comparing treatment No. 2 with treatment No. 5. Treatments No. 3 and No. 6 clearly show the undesirable structure which results from slow cooling through the critical temperature range.

27. Strength and static ductility of each of the six test blocks

is lowest at the center and highest at the surface. This condition is always found in massive sections and is variously attributed to differences in metal density, variations in cooling rates, center line shrinkage, or segregation of sulphur, phosphorus, silicon, or carbon. None of these causes seems to apply here as the chemical composition determined from samples at various locations in one of the 10- x 10- x 20-in. castings, Table 10, shows no significant segre-

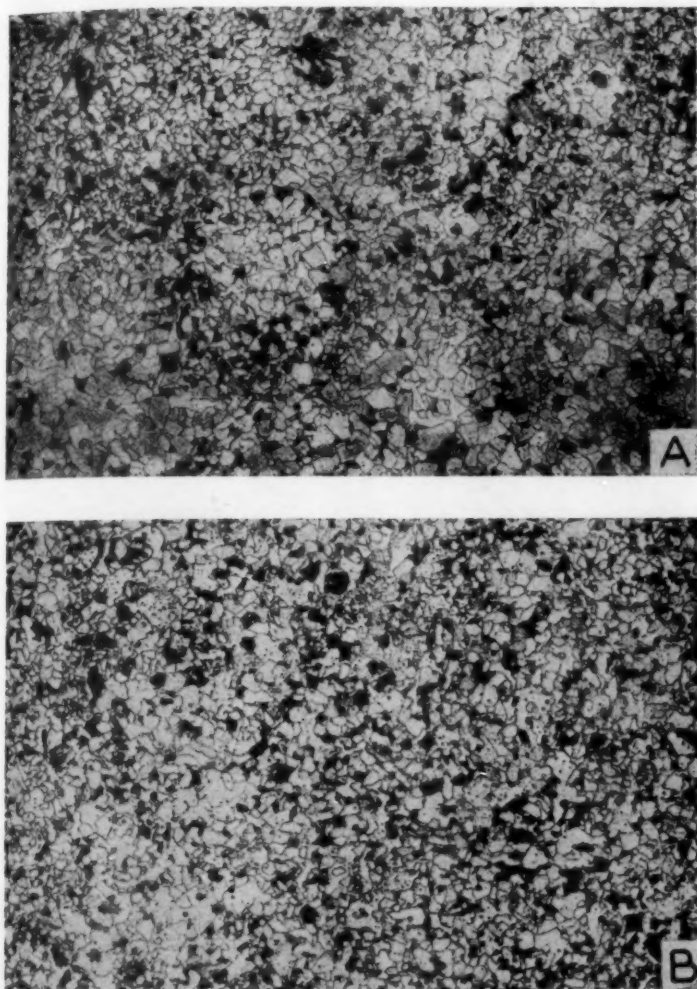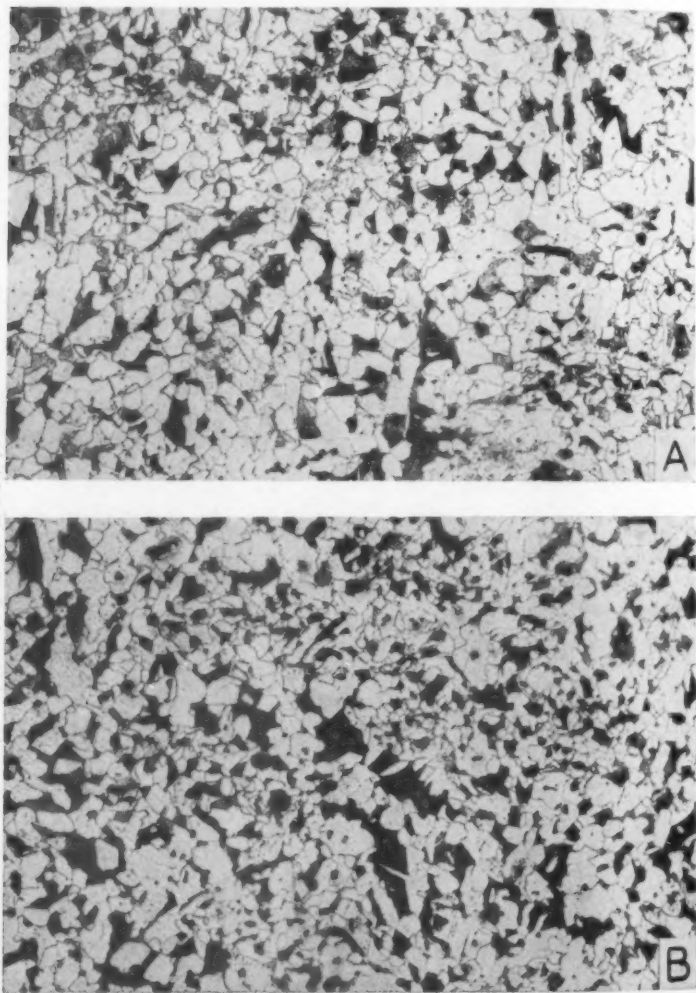

FIG. 12—PHOTOMICROGRAPHS OF (A) CENTER AND (B) CORNER OF 10- x 10- x 20-IN. CAST-
ING GIVEN TREATMENT NO. 2. MAGNIFICATION x100.

gation, deep etching discloses no center-line porosity, and density measurements taken on one of the 10- x 10- x 20-in. castings, Table 11, show only a negligible difference between the edge and center of the block. Sulphur evidently is not responsible for this condition as a sulphur print taken diagonally across a section of one of the large test castings showed uniform sulphur distribution.

Table 10

CHEMICAL COMPOSITION AT 3 LOCATIONS IN ONE OF THE 10x10x20-IN. CASTINGS FROM HEAT R

| | Per Cent | | |
	Center	Midway between Center and Corner	Corner
Carbon	0.28	0.24	0.25
Manganese	0.59	0.61	0.61
Silicon	0.43	0.42	0.43
Phosphorus	0.029	0.030	0.033
Sulphur	0.015	0.014	0.017

Table 11

EFFECT OF MASS ON DENSITY

Location of Sample	Density
Corner, 10x10x20-In. Casting	7.822
Center, 10x10x20-In. Casting	7.821
Corner, Coupon B	7.830

Table 12

CRITICAL POINTS OF SAMPLES TAKEN FROM 10x10x20-IN. CASTINGS*

| | Heat R | | Heat S | |
	Center °F.	Corner	Center °F.	Corner
Ac_1	1375	1380	1365	1375
Ac_3	1540	1540	1535	1520
Ar_3	1375	1380	1390	1365
Ar_1	1205	1190	1195	1195

28. It is of interest to note that the static ductility values of specimens taken from the bottom of type A test coupons are consistently higher than those taken from the top of the same coupons. This variation is in the order of 3 per cent elongation and 15 per cent reduction of area. Such a consistent and large variation should preclude the use of this type of coupon for test purposes.

* Determined dilatometrically at heating and cooling rates of approximately 40°F. per min.

FIG. 13—PHOTOMICROGRAPHS OF (A) CENTER AND (B) CORNER OF 10- x 10- x 20-IN. CAST-
ING GIVEN TREATMENT NO. 3. MAGNIFICATION x100.

29.   Inclusion studies were made on all microspecimens before
etching and there were no signs of stringers or inclusion concen-
trations of a type which would impair ductility. Figure 17 shows
photomicrographs of typical inclusions found.

30.   A photomicrograph of one of the deep-etched sections is
reproduced in Fig. 18. Hair line cracks are apparent. However,
such cracks are frequently encountered upon deep-acid etching

and are no reflection on the integrity of the steel. The etched surface was machined from one of the sections and it was then magnafluxed. No evidence of cracking appeared.

CONCLUSIONS

31. The long annealing cycle for medium carbon steel castings of moderately heavy sections can be replaced by a shorter heat
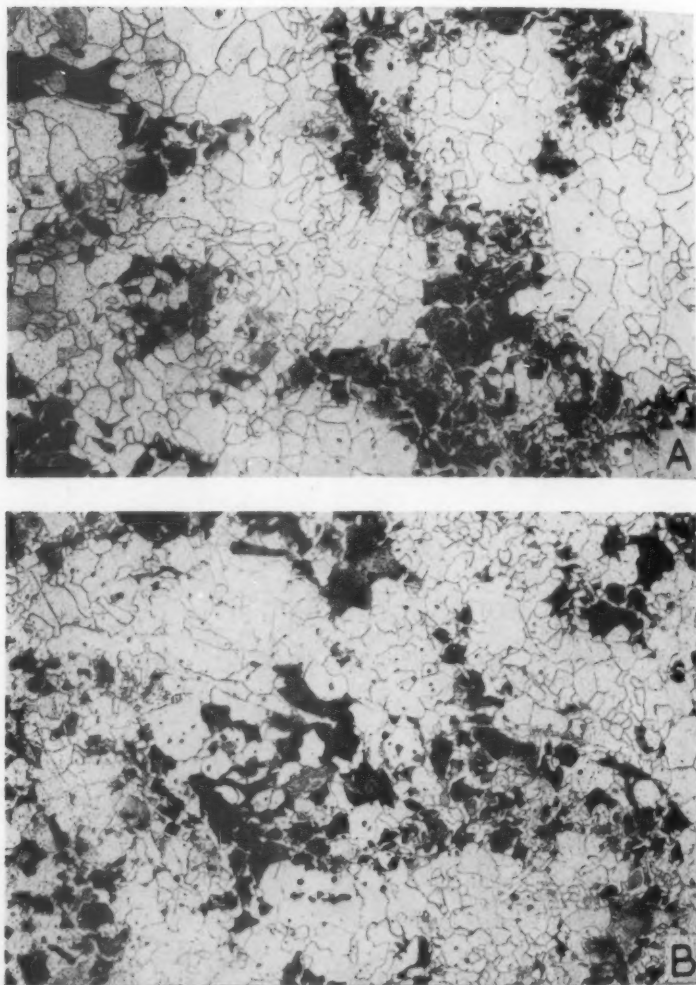

FIG. 14—PHOTOMICROGRAPHS OF (A) CENTER AND (B) CORNER OF 10- x 10- x 20-IN. CASTING GIVEN TREATMENT NO. 4. MAGNIFICATION x100.

FIG. 15—PHOTOMICROGRAPHS OF (A) CENTER AND (B) CORNER OF 10- x 10- x 20-IN. CAST-
ING GIVEN TREATMENT NO. 5. MAGNIFICATION x100.

treatment of approximately one-third the time without detriment
to physical properties. In fact, the yield ratio, reduction of area
and notched-bar impact resistance can be raised materially with-
out adversely affecting the tensile strength.

32.   Homogenizing, air cooling, reheating for grain refinement,
water quenching and full tempering (treatment No. 4) is more
effective in obtaining a good combination of strength and ductility

than is full annealing or single normalizing and tempering. Double normalizing, including homogenization and grain refinement, followed by tempering (treatment No. 1) can be regarded as a second choice of the treatments applied.

33. For castings having uniform sections and of composition comparable to those used here, heating rates up to 400°F. per hour do not appear to be harmful.
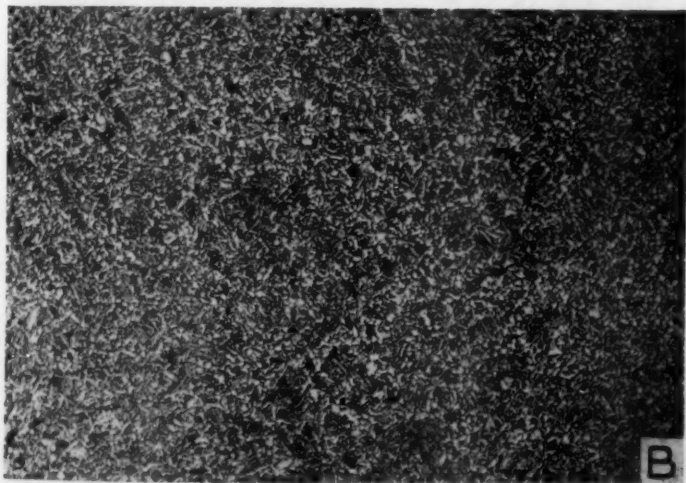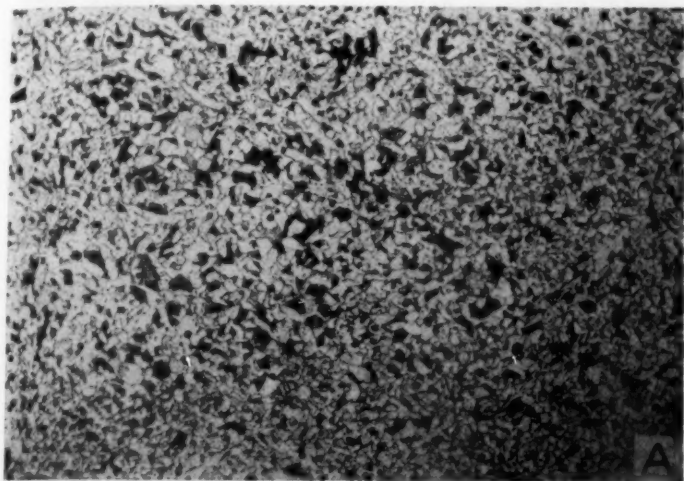

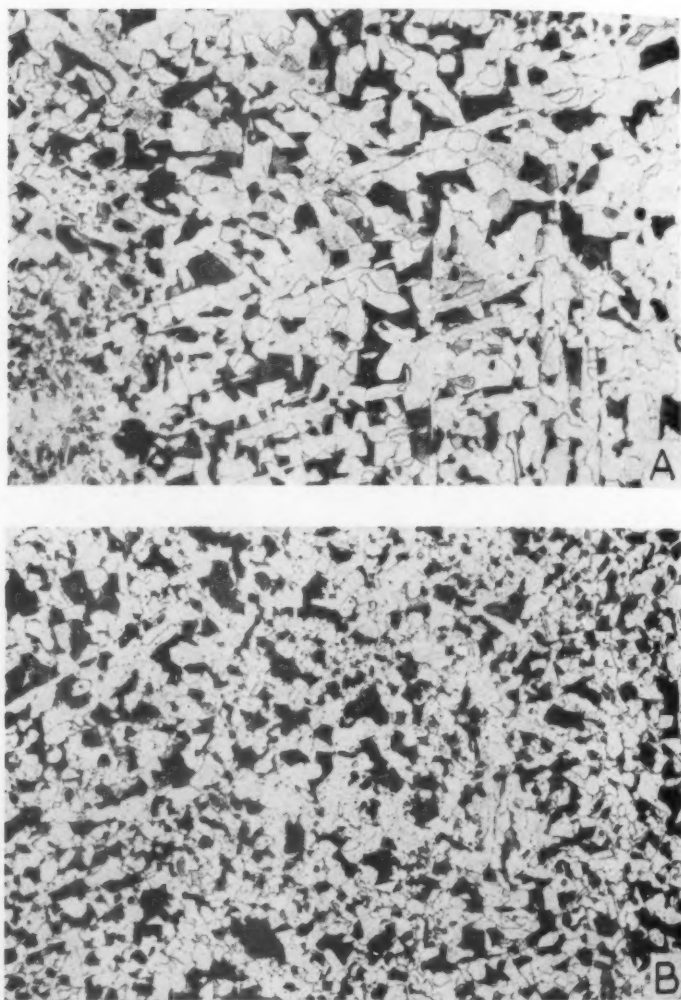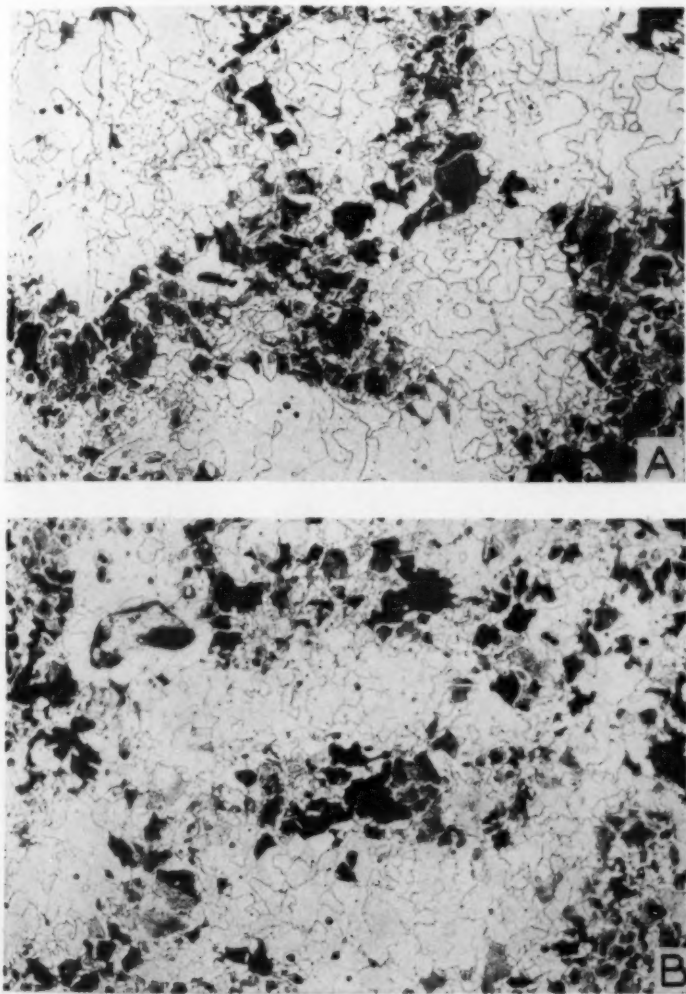
Fig. 16—Photomicrographs of (A) Center and (B) Corner of 10- x 10- x 20-In. Casting Given Treatment No. 6. Magnification x100.
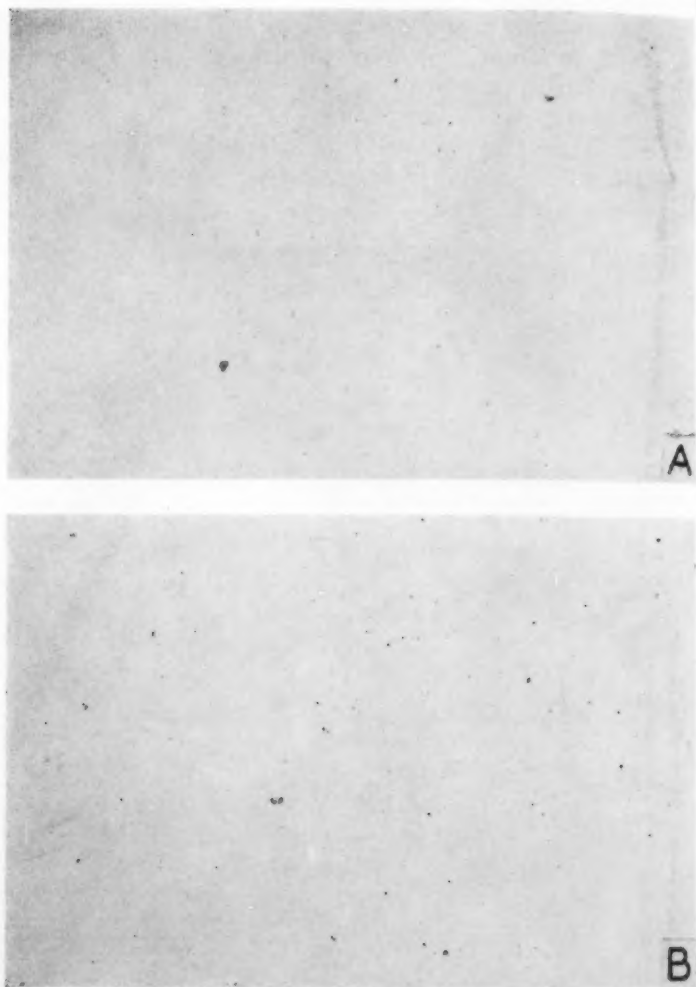
FIG. 17—TYPICAL NON-METALLIC INCLUSIONS FOUND AT (A) CENTER AND (B) CORNER OF 10- x 10- x 20-IN. CASTING. UNETCHED. MAGNIFICATION x100.

PLANS FOR FUTURE WORK

34. Despite the savings in time and improvement of physical properties resulting from treatment No. 4 and, to a lesser extent, from treatment No. 1, the fact remains that some steel foundries would be seriously handicapped if required to adhere to such heat treating practice. A great number of heat treating furnaces in current use are not designed to operate above 1650°F. or
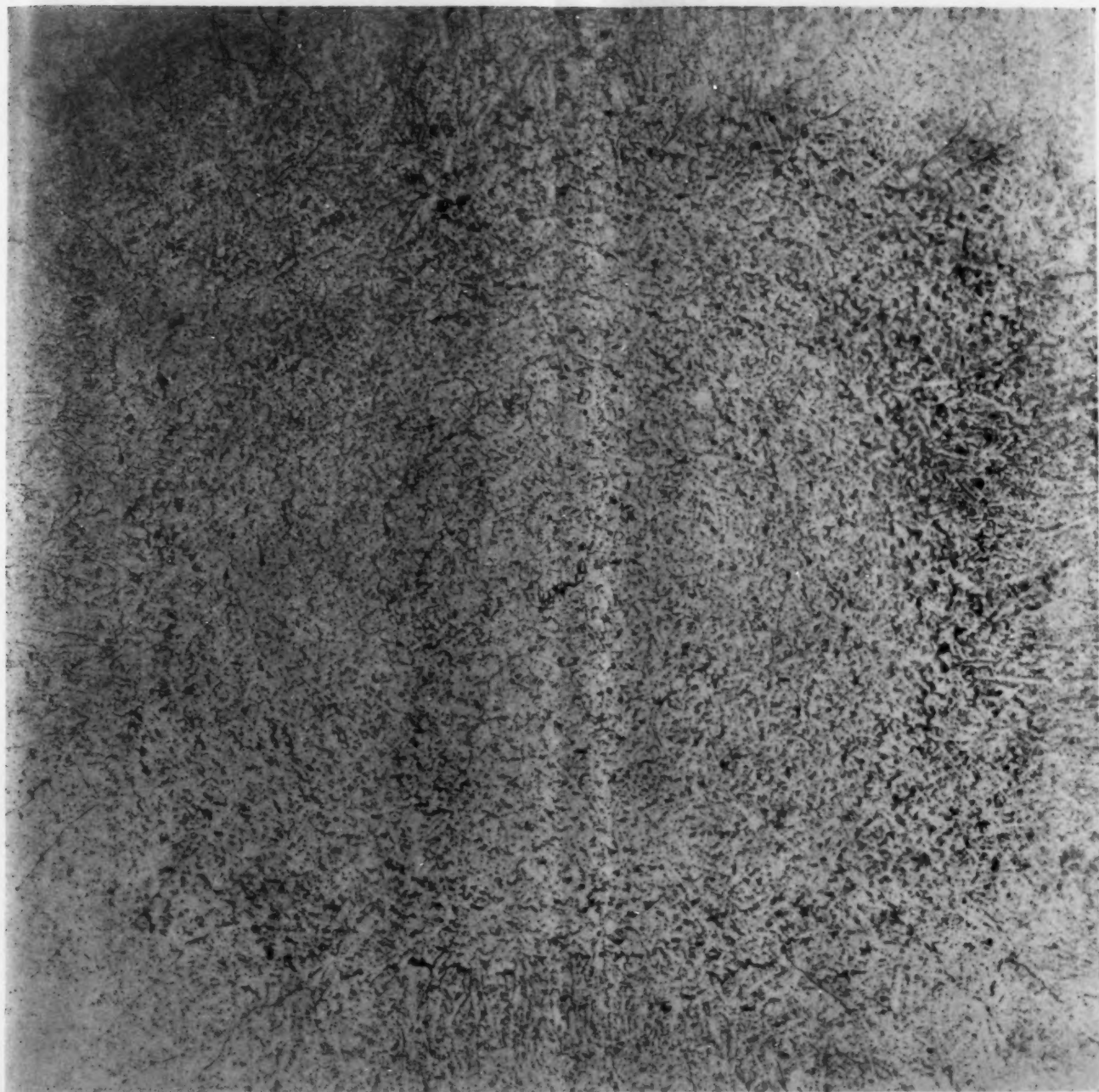
Fig. 18—Typical Macrostructure of Cross Section of 10- x 10- x 20-In. Casting. Etched in 50 Per Cent Boiling Hydrochloric Acid. Magnification x ⅜.

1700°F., and a number of foundry heat treating departments are not equipped for liquid quenching. Furthermore, batch heating is conducive to rather large temperature gradients throughout a charge. Unless adequate pyrometric equipment is available to assure that all castings have reached the desired temperature for the desired time, holding at the heat treating temperature for the minimum time might become a dangerous practice.

35. With these possible limitations in mind, a new series of

FIG. 19—IMPACT RESISTANCE CHARPY KEYHOLE SPECIMENS.

heat treatments is being given to six 10- x 10- x 20-in. castings and corresponding coupons poured from steel of nearly the same composition as the heats previously used. These castings and coupons will be sampled and tested in the same manner as those from heat S after heat treatments as given in Table 13.

Table 13

HEAT TREATMENTS

Heat T

Treatment No. 7,
    1650°F., one hr./in. of section, AC to 800°F.
    1250°F., one hr./in. of section, AC
      Approx. total time involved— 28 hrs.

Treatment No. 8,
    1650°F., one-quarter hr./in. of section, AC to 800°F.
    1250°F., one-quarter hr./in. of section, AC
      Approx. total time involved— 13 hrs.

Treatment No. 9,
    1800°F., one hr./in. of section, AC to 800°F.
    1650°F., one hr./in. of section, AC to 800°F.
    1250°F., one hr./in. of section, AC
      Approx. total time involved— 43 hrs.
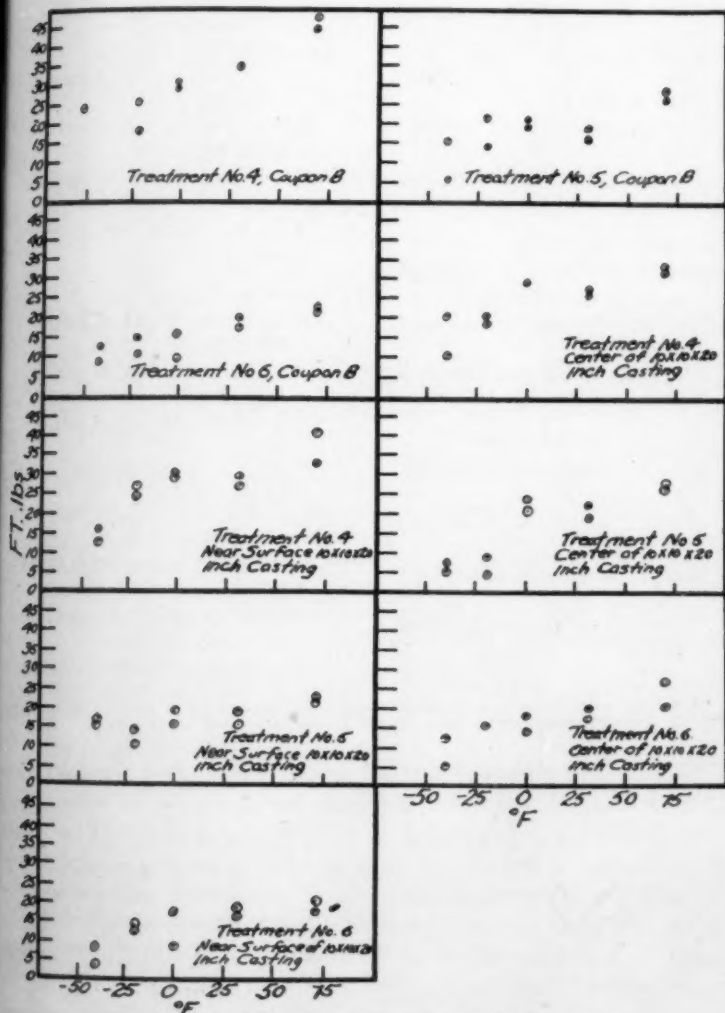
Heat T

Treatment No. 10,
    2000°F., one hr./in. of section, AC to 800°F.
    1650°F., one hr./in. of section, AC to 800°F.
    1250°F., one hr./in. of section, AC
      Approx. total time involved— 44 hrs.

Treatment No. 11,
    1650°F., one hr./in. of section, AC to 800°F.
    1650°F., one hr./in. of section, AC to 800°F.
    1250°F., one hr./in. of section, AC
      Approx. total time involved— 42 hrs.

Treatment No. 12,
    1600°F., 25 hours, FC to 865°F., AC
      (Heating time, 15 hours, cooling time, 24 hours)
      Total time involved—64 hours

NOTE: No restrictions are being imposed upon heating rates except for treatment No. 12. Heating rates in the order of 400°F. per hour are being used.

36. It is to be noted that all treatments, except treatment No. 12 which is the control treatment, are based upon normalizing from 1650°F. and tempering at 1250°F. All treatments are based upon holding times of one hour per in. of section except No. 8. No. 8 is directly comparable to No. 7 and should indicate whether or not one hour per in. of section allows an unnecessary factor of safety in heat treating time. Treatments No. 9 and No. 10 appear to be contradictory to the statement that heating above 1650°F. is considered undesirable from the standpoint of uniform commer-

cial practice at present. These treatments are being given to obtain information regarding the advisability of utilizing higher temperature homogenizing treatments when and if they become more generally possible. Treatment No. 11 is being applied in an attempt to determine what optimum properties can be obtained without resorting to higher temperatures than 1650°F. or to liquid quenching.

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DISCUSSION

Presiding: R. A. GEZELIUS, General Steel Castings Corp., Eddystone, Pa.

Co-Chairman: JOHN HOWE HALL, General Steel Castings Corp., Eddystone, Pa.

J. B. CAINE[1]: Can the authors give any more details regarding the aluminum addition, how much was added and the residual metallic aluminum content? Were any silica inclusions present, or were all the oxide inclusions alumina?

This point may be important, as it is at least probable that a fine grained aluminum killed steel will react differently during heat treatment, especially a homogenizing treatment, than a coarse grained silicon killed steel.

It has been the writer's experience with lighter sections than those studied by the authors, that an aluminum killed steel, even as cast, is relatively fine grained when compared with a silicon killed steel as cast. These fine grained cast steels respond to a homogenizing heat treatment much faster than the coarse grained cast steels.

If this is true for the larger sections studied by the authors, and the steels studied were fine grained aluminum killed steels, the results may only apply to these fine grained aluminum killed steels.

MR. CLARK: I believe they were chiefly aluminum inclusions. It was basic electric steel, and I think 2 lb. of aluminum per ton addition was made for final deoxidation.

CHAIRMAN GEZELIUS: One of the interesting things, as is pointed out, is the amount of time that we might save by using heat treatments of this character. Unfortunately, they are not of much value to us unless the specification writers also know that this work is being done. Has the Navy Department taken official cognizance of this very interesting paper?

C. L. FREAR[2]: This paper is a partial report of a research project initiated by the Navy Department to determine the actual effect of various heat treatments on the physical properties, including low temperature impact resistance, of steel castings in heavy sections. Bureau of Ships specification for carbon steel castings already permits normalizing followed by suitable temperature in lieu of annealing as originally required. If the results of further tests prove its expected advantage, this treatment will probably be extended to other specifications which now require straight annealing.

H. D. PHILLIPS[3]: We know it is beneficial to all properties of steel castings to get away from the old idea of heating up to 1650°F., holding for a long time and cooling very slowly. We could not make certain types of steel castings if we did not take advantage of the improvements that we obtain from going to high temperatures for homogenizing and then giving them liquid quenches from the hardening and tempering temperatures. However, what about the stresses left in the cast-

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[1] Sawbrook Steel Castings Co., Lockland, O.
[2] U. S. Navy Department, Washington, D. C.
[3] Lebanon Steel Foundry, Lebanon, Pa.

ings? We know from our experience that it is a question whether we can ever relieve all the stresses in a steel casting. We can, we believe, equalize the stress in steel castings if we quench a casting from the hardening and tempering treatment, then give it a low temperature draw or tempering treatment. There is a real advantage in quenching from the tempering treatment with low alloy steels due to the definite improvement in impact properties. If we take advantage of those treatments to secure good physical properties in our test bars, or pieces cut from castings and then tested, how is the casting going to behave in service? In actual operating experience the stresses left in castings usually are not of such magnitude that they impair the serviceability. If they do, there are treatments still available to us, such as the low temperature draw. We can equalize the stresses so that when the casting is placed in service it will not distort nor will it result in a brittle failure under sudden loading.

JOHN JUPPENLATZ[1]: What does homogenization do? In our opinion, it causes a movement of dendritic segregation of carbides, phosphides and grain boundary substances such as sulphides by diffusion at temperatures of austenization. To be effective, temperatures must be high enough to first promote diffusion of the primary austenitic grain boundary materials which act as barriers to subsequent diffusion of carbides into the ferrite areas. Effective homogenization has the effect of dispersing the carbide-rich areas into the ferrite-rich areas, so that a uniform and well dispersed structure results upon cooling. With temperature being of prime importance, and time secondary, the rate of cooling should be sufficiently rapid through the critical ranges to prevent reversion of the carbides and ferrite into massive or rich areas, respectively, which are difficult to diffuse when reheating for grain refinement just above the critical ranges.

The impact tests as noted in this paper were very interesting, and one thing that stood out was the fact that some of the impact values of bars taken from center sections of castings were higher than those from bars taken from and near the surface. We have found this same phenomenon, and we believe that this condition is attributable to the dendritic formation upon solidification resulting in directional and massive dendrites, with more concentrated ferrite and carbide interstices near the surface. These surface dendrites form in a perpendicular manner to that surface, and are parallel to each other from that surface for some distance, finally merging into a more or less equi-axed dendritic formation which is well dispersed, having little or no directional pattern which is often distorted due to directional feeding from the riser during solidification. This reason can account for higher impact properties of center sections of castings when conditions of heat treatment are ideal for these sections.

Another phase of interest was the low-temperature impact properties, and the authors have brought out that when a structure has large, massive ferrite areas, and rather strong carbidic areas, the impact properties at low temperatures fall off quite rapidly. That brings up a point

which is very important in steel foundries today, and that is the proper heat treatment so that structures are rather homogeneous, and so that excellent impact properties can be maintained at these lower temperatures.

MR. CLARK: In connection with the matter of movement of non-metallics by homogenizing treatments, we have not been able to observe any effect of movements of sulphides or other types of non-metallics by high temperature homogenizing treatments.

MR. JUPPENLATZ: Homogenizing temperatures in the vicinity of 1800°F. and below apparently do not tend to move the non-metallics, such as, we will say, grain boundary sulphides or that class of chain-like material. However, higher temperature of 1950, 2050 or 2150°F., seem to do the work. We have found definite movements of, particularly, the sulphide chain networks, if they are present at these temperatures. Of course, these higher temperatures do cause quite a diffusion of the carbides, but unless the prime austenitic grain boundary barriers are diffused or moved around, it is very difficult to cause a diffusion of the carbides into the ferrite planes through barriers such as the sulphide chain network presents.

MESSRS. CLARK, BISHOP AND TAYLOR *(authors' closure):* The question raised by Mr. Gezelius seems amply answered by Mr. Frear, although we might add that the proposed treatments outlined in the text will be completed before recommendations for specifications will be made, but the work is well under way.

We are in accord with Mr. Phillips' views regarding the use of homogenizing treatments and liquid quenches from proper hardening temperatures. Quenching from the tempering temperature is effective in two ways, namely, (1) prevention of temper brittleness in those steels which are subject to it, and (2) establishing most favorable stress gradients. With regard to the latter, considerable work on stress relief has been done at the Naval Research Laboratory and Mr. Rominski will present a paper this evening on the subject.

Mr. Juppenlatz' explanation of the difference in notched-bar impact resistance between exterior and interior portions of a heavy casting appears very plausible.

We wish to thank the participants in this discussion for their pertinent comments.

A Study of "Burnt-On" or Adhering Sand

By J. B. Caine*, Lockland, Ohio

Abstract

*"Burnt-on" or adhering sand is an old foundry prob-
lem encountered in all foundries, and particularly in steel
foundries. Often a severe case of adhering sand is re-
sponsible for the scrapping of a sound casting. The
author shows that "burnt-on" sand is the result of metal
penetration of large voids in the sand, followed by freez-
ing of the metal, which mechanically locks the sand to
the casting. In a highly refractory sand, voids may be
present on ramming as the result of large grain size. In
less refractory sands, voids may be formed by the fusing
of the colloidal matter and finer silica particles present in
the sand. Metal will penetrate either type of void pro-
vided the sand temperature is higher than the melting
point of the casting alloy. Metal penetration may also be
reduced or prevented either by the formation of a continu-
ous, extremely viscous fused film such as results when hot
metal strikes a properly applied silica wash, or by gas
pressure such as results when metal heats an organic
binder incorporated in the sand mix. The author also
discusses the effect of impurities which may be present in
either the clay or the silica fraction of a sand mix.*

Introduction

1. The problem of "burnt-on" or, more properly, adhering
sand, is one that has confronted the foundry industry since its in-
ception. Although adhering sand is encountered in all branches
of the industry, it is a greater problem in the ferrous foundry than
in the non-ferrous foundry, and particularly in the steel foundry
because of the higher pouring temperatures encountered there.

2. *Theories Explaining "Burn-On."* Every foundryman has
his own theories about this problem, and it seems that no two
theories agree. Dirker[1] has advanced a theory explaining adhering

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sand by assuming the formation of iron silicate. He believes that
iron oxide is formed first as the result of oxidation of the metal by
air present in the voids of the sand; this iron oxide then reacts
with the silica of the sand grains to form a liquid iron silicate. He
finds that the melting point of iron silicate when heated in a finely
divided state is low, and explains that "burnt-on" sand is the re-
sult of this low-melting-point constituent becoming fluid and ad-
hering to the casting. Goodale[2] extends this theory further and
gives diagrams showing the supposed mechanism of "burn-on."
He explains the occurrence of metallic iron in adhering sand by as-
suming that the iron silicate away from the sand-metal interface is
reduced to metallic iron. It is interesting to note that in neither
case has the author determined the existence of iron silicate in the
adhering sand. Actually this theory is not supported by experi-
mental evidence. If iron oxide and iron silicate are formed at the
sand-metal interface, it should be possible to determine how much
is present by chemical analysis. Then, as the sand is used over and
over again, the percentages of iron oxide and iron silicate should
increase. A number of steel foundries in the middle west have
checked this point and can find no increase. In every case, the
total iron content of their used sand was between 0.5 per cent and
1.0 per cent, the actual content being determined not by how often
the sand had been used, but by the total iron content of the new
sand as received.

3. *Correlation of Sintering Point and "Burnt-On" Sand.* As
was brought out in the discussion of Goodale's paper, the problem
must be more complicated than can be explained by the simple
theory of fusion and adherence of iron silicate to the casting. Al-
though the very name "burn-on" denotes a heat effect involving
fusion or at least sintering and adherence due to this fusion or
sintering, every foundryman has seen cases of mechanical penetra-
tion which, to all appearances, look exactly like "burnt-on" sand
that is caused by fusion of some material in the sand. Any num-
ber of experiments have been performed in the past to correlate
the behavior of sand in the foundry with the fusion or sintering
point of the sand. In most cases, no definite correlation could be
found between the fusion point as determined by the Sager cone
test or the A.F.A. sintering test and the amount of "burnt-on"
sand in the foundry. In some cases a lowering of the sintering
point was accompanied by an increase in the amount of "burnt-
on" sand. In others, a lowering of the sintering point was ac-
companied by a decrease in the amount of "burnt-on" sand.

4. There are at least two reasons for this confusion. First, there is no reason to assume that "burnt-on" or adhering sand is caused by only one variable such as fusion. Second, until recently, there has been no standard method of measuring the refractoriness of molding materials. The Sager or pyrometric cone equivalent test which is standard for fire clay and silica refractories, has been found, both in this country and in England[3], to be almost worthless as a test for the refractoriness of molding materials. It is a test for the softening or melting point of the mass as a whole. As will be shown later, serious trouble is experienced in the foundry long before this point is reached.

5. The sintering test as advanced several years ago by the American Foundrymen's Association was not sufficiently standardized to yield consistent results, especially when the results compared were from different laboratories. Recent work of the Sintering Test Subcommittee should overcome this last difficulty[4, 5].

6. *Scope of Report.* This report is divided into two main sections. The first is a study of the mechanism of "burnt-on" or adhering sand. The second is a quantitative study of the materials present or added to the sand mixes used in the foundry, and their effect on the sintering points and behavior of the sand mixes when subjected to liquid steel.

7. As all of the work was done in a steel foundry, the effect of only molten steel on the various sands was studied. However, a number of non-ferrous and iron sands are included in this report. Although the conclusions drawn from this work apply directly only to steel practice, the fundamentals should apply to all branches of the foundry industry.

Experimental Procedure

8. *Preparation of Sand Mixes.* In all, several hundred sand mixes were made and studied. All mixes, except those used in the writer's foundry and those received from other foundries, were mixed in a laboratory muller of 15-lb. capacity. If the dry binders were finely divided, the sand and binders were mixed dry for one minute; water was then added to bring the moisture content of the mix to the proper percentage, and the mix was mulled for 3 min. This cycle was chosen because it gave results comparable to those obtained using large mullers in the foundry. If the material to be added to the sand was not available as a fine powder, it was dissolved in water if it was soluble, or added as a slurry in order to coat the sand grains uniformly.

9. *Methods of Testing.* The sands were tested according to the standard methods of the American Foundrymen's Association. Room temperature properties were determined for each mix. Only a few pertinent properties are included in this report as others show nothing that has not been reported before[6].

10. *Determination of Sintering Points.* Sintering points of the various sand mixes were determined according to the revised pro-
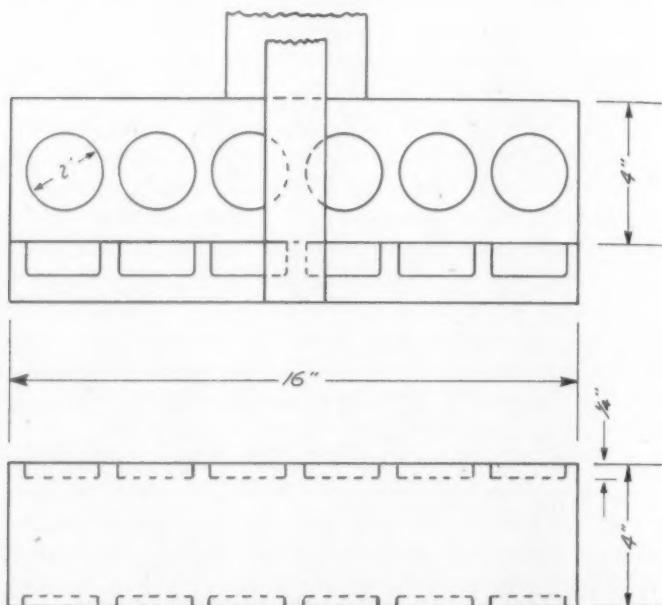
FIG. 1—DETAILS OF TEST BLOCK.

cedure as recommended by the Sintering Test Subcommittee of the American Foundrymen's Association[4]*. Both the "A" and "B" sintering points are given in the tables for the record, but only the "B" sintering point has been plotted in most of the graphs, as no correlation could be found between the "A" sintering point and adhering sand. The effect of molten steel on these sand mixes was studied by making a casting as shown in Fig. 1. Standard 2-in. diameter by 2-in. high A.F.A. test cores were inserted in the mold and subjected to the action of molten steel at about 3000°F. As

* The Sintering Test Subcommittee defines the "A" and "B" sintering points as follows: The "A" sintering point is the lowest temperature at which the ribbon makes a "V" when lifted off the sand specimen. The "B" sintering point is the lowest temperature at which smaller grains can be seen to start to fuse at low magnification, 20 to 25 diameters.

can be seen from Fig. 1, the cores extended $\frac{1}{4}$-in. into the casting cavity. This was done to protect the sand in contact with the metal when the casting was shaken-out, so it could be examined.

11. *Control of Test Specimens.* As the pouring temperature of metal is one of the major variables affecting adhering sand and the effect is greater the closer the pouring temperature approaches the freezing temperature of the metal, the casting was poured at a higher than normal pouring temperature for the 4-in. section used. Because this investigation extended over a period of years, it was impossible to pour all the test blocks in the same heat; so in order to be sure that no unexpected variable in the metal influenced the results, a control core, usually a pure silica sand-bentonite mix, was cast with each block. In this report, all tests cast in the same block are designated by the same prefix letter. For example, tests A1 to A12 were cast in the same block, tests B1 to B12 in another block, etc. Even though every effort was made to keep the pouring temperature of the metal constant, it varied from 2980°F. to 3060°F. As will be seen later, even this small variation in pouring temperature has an appreciable effect on the amount of adhering sand, so that the results obtained from the various blocks are not strictly comparable. In order to compare cores poured in different tests blocks, the temperature of the metal and the amount of adhering sand on the control core must be considered.

THE MECHANISM OF ADHERING SAND

12. In order to study the mechanism of adhering sand, a large number of samples were taken from production castings in order to compare those that showed a clean surface with those that showed various degrees of adhering sand. It was found that visual examination was not precise enough for quantitative evaluation, and it was necessary to use microscopic examination.

13. *Preparation and Description of Photomicrographs.* Specimens for all photomicrographs appearing in this report were prepared according to the method outlined in the 1942 report of the Sintering Test Subcommittee[5]. As was explained in the report, two samples using two methods of illumination are necessary to make a quantitative study of what is happening at the sand-metal interface when molten metal comes in contact with a molding material. The first two photomicrographs in this report, Figs. 2a and 2b, are representative of those throughout this report, and a detailed explanation of these applies to all other photomi-

FIG. 2a—SAND SURFACE IN CONTACT WITH METAL AT 2900°F. GOOD PEEL. X25
OBLIQUE ILLUMINATION.
FIG. 2b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 2a. PENETRATION
.012 INCH. X25. VERTICAL ILLUMINATION.

crographs that follow.

14.   Figure 2a is a photomicrograph of a sand surface in contact
with metal, using oblique illumination. The specimen has been
mounted in bakelite so as to preserve the exact position of the sand
grains. With oblique illumination, the sand grains appear light,

the voids dark. Figure 2b is a photomicrograph of a section at right angles to the sand-metal interface and to the sand surface shown in Fig. 2a. Unfortunately, the reflectivity of sand and steel is so different that the same type of illumination cannot be used for both sections, requiring the use of two types of illumination and two photomicrographs. Therefore, the photomicrographs of the sand-metal interface have been taken with vertical illumination, reversing the light and dark as compared with those using oblique illumination. With vertical illumination, the steel is light and the sand dark, with very little contrast between the sand grains and the voids. The mottled areas at the top of the photomicrographs of the sand-metal interface are lead used to mount these specimens.

15. *Steel Facing Sand Containing 2.0 Per Cent Bentonite and 1.5 Per Cent Cereal Binder.* Figures 2a and 2b show the sand sur-
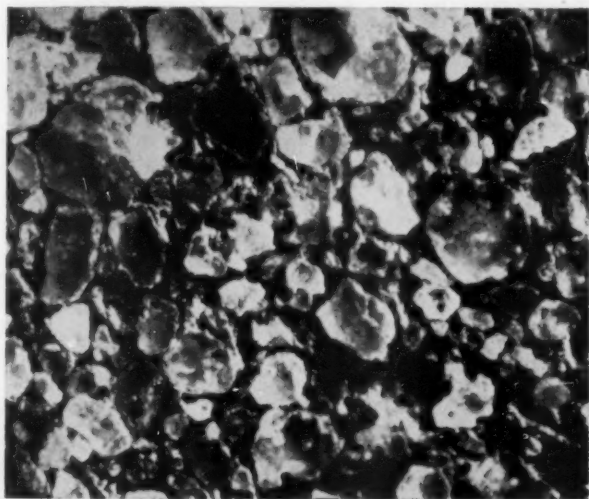
FIG. 3—SAND AS RAMMED. X25. OBLIQUE ILLUMINATION.

face and sand-metal interface of a casting that peeled perfectly on shake-out. This casting, weighing 500 pounds, poured at 2900°F., and taken from production, was molded in a typical steel facing sand containing 2.0 per cent bentonite and 1.5 per cent cereal binder added to a crude Ohio sand with an average A.F.A. grain size of 56. The "B" sintering point of this mix was 2850°F. Figure 2a, showing the sand surface in contact with the steel, shows a surprising amount of fusion for what would be considered a high

sintering-point sand. If Fig. 2a is compared with Fig. 3, a photo-micrograph of the same sand as rammed, it will be seen that nearly all the grains have fused, and only the larger grains remain un-affected by the heat. However, in spite of this fusion, the sand peeled perfectly from the casting on shake-out. As shown in Fig.
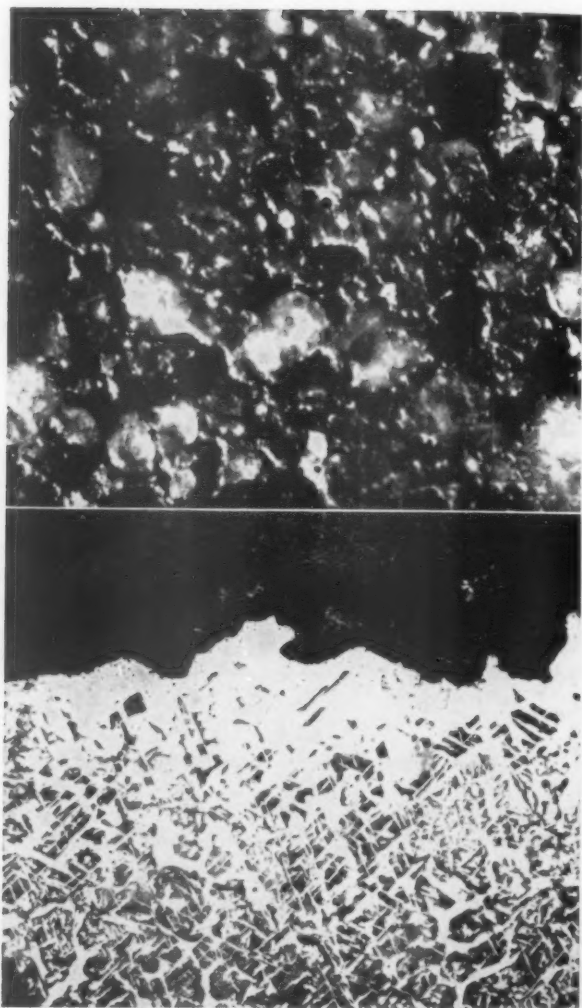

FIG. 4a—SAND SURFACE IN CONTACT WITH METAL AT 2940°F. X25. OBLIQUE ILLUMINA-TION.
FIG. 4b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 4a. PENETRATION .080 INCH. BAD "BURN-ON." X25. VERTICAL ILLUMINATION.

2b, the surface of the casting is relatively smooth with little or no penetration of the steel into the voids of the sand. The surface of the casting appears smooth on visual examination.

16. *Effect of Increased Pouring Temperature.* Figures 4a and 4b show the sand surface and sand-metal interface of a similar casting poured at a slightly higher temperature of 2940°F. Sand adhered to this casting on shake-out and was hard to rem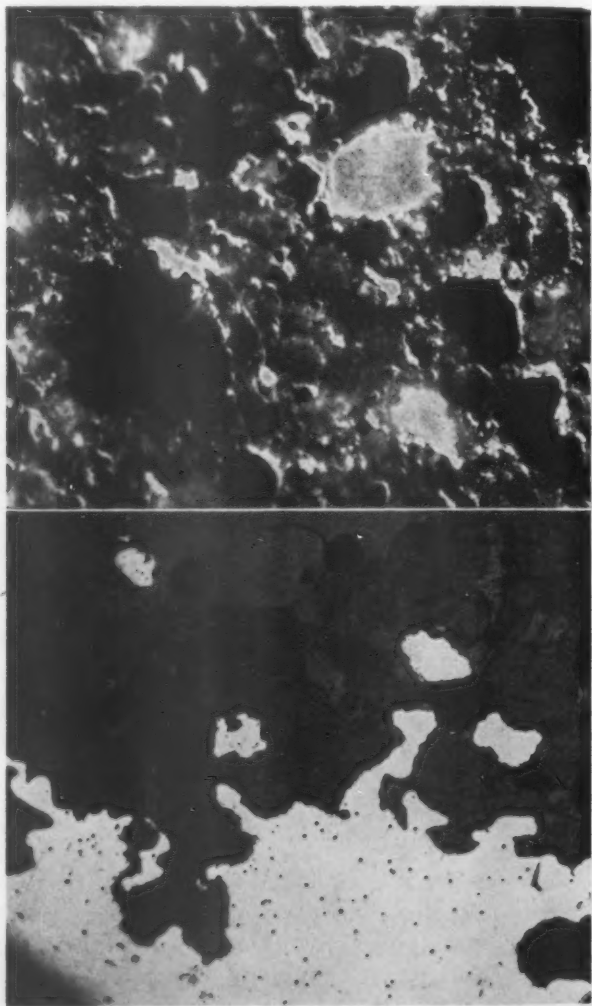ove by blasting. The sand surface in contact with the steel shows slightly more fusion than that shown in Fig. 2a, but the size of the voids has increased appreciably. A similar change is seen at the sand-metal interface. Here, as shown in Fig. 4b, the metal is starting to penetrate the sand, locking it to the casting. The increase in penetration due to the increase in void size is much greater than the increase in fusion. Figure 4b shows the light areas of steel penetrating the dark sand mass.

17. Both castings represented by Figs. 2 and 4 were poured in exactly the same sand, the only difference being a slight increase
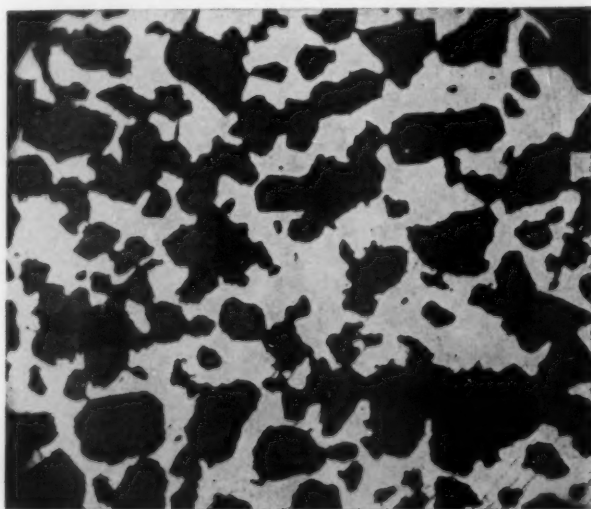
FIG. 5—VERY BAD "BURN-ON." PENETRATION .25 INCH. X25. VERTICAL ILLUMINATION.

of 40°F. in pouring temperature. This increase in pouring temperature was sufficient to open up slightly larger voids in the sand for the second casting. These voids were large enough and the sand hot enough to allow the steel to penetrate, resulting in "burnt-on" sand.

18. If the temperature of the metal is increased further, all

other variables remaining constant, penetration increases tremendously as can be seen from Fig. 5. Here we have complete penetration exceeding the width of the field under the microscope at this magnification. The smaller grains of sand have disappeared completely, the voids becoming very large. The sand-metal mixture
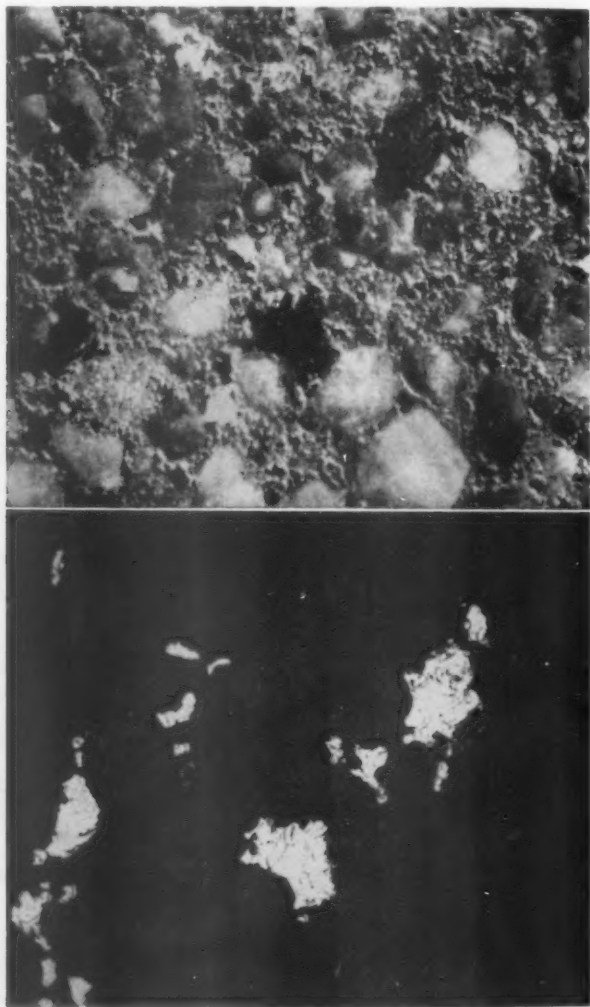
FIG. 6a—SAND SURFACE IN CONTACT WITH MOLTEN IRON AT 2530°F. X25. OBLIQUE ILLUMINATION.
FIG. 6b—SAME AS FIG. 6a, BUT WITH VERTICAL ILLUMINATION, SHOWING PENETRATION INTO LARGER VOIDS.

adhering to the casting is a mechanical mixture of the larger grains
of the sand and steel.   When the condition illustrated in Fig. 5 is
even approached, one has a very serious cleaning problem, usually
necessitating scrapping the casting.

19. *Formation of Voids as the Result of Fusion.*   The reason
for this penetration can be seen in Figs. 6a and 6b, photomicro-
graphs of a sand starting to fuse and adhere to the casting.   As
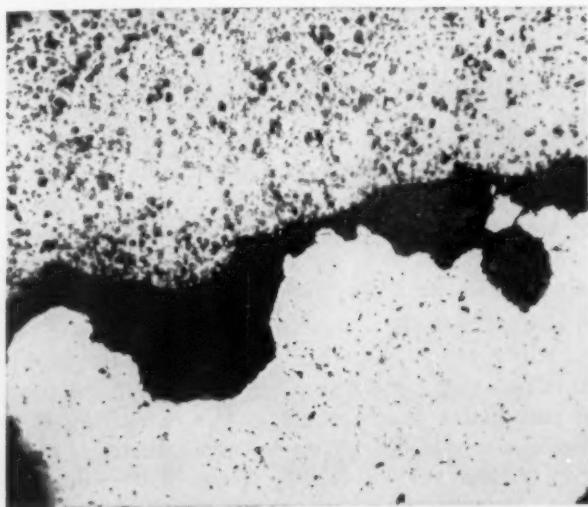
Fig. 7—Section Through Sand-Metal Interface Shown in Figs. 6a and 6b. Penetra-
tion .032 Inch.   X25.   Vertical Illumination.

this same action has been shown before for steel[7], an iron sand has
been selected for this illustration. Figure 6a shows the sand surface
in contact with the iron at 2530° as seen with oblique illumina-
tion.   The colloids present in this natural-bonded iron sand have
started to fuse, but the silica grains have not been affected by the
temperature of molten iron.   Originally the spaces between the
silica grains were completely filled by these colloids.   Now some
voids have opened up.   When this same area is examined under
vertical illumination as in Fig. 6b, it can be seen that the larger of
these voids which have opened up due to fusion, have been pene-
trated by the iron, thus locking the sand to the casting. Figure 7
shows the sand-metal interface of the same casting.   Here, again,
we have the same type of penetration that was seen in steel sands.

20. *Cause of "Burn-On."* The mechanism of penetration of
metal into sand and thus locking it to the casting was found to be

the fundamental cause of ''burnt-on'' or adhering sand in all the hundreds of samples examined. In fact, the amount of adhering sand can be measured quantitatively by measuring the amount of penetration. In every specimen examined, when the penetration was less than .005 in., the castings peeled perfectly on shake-out, with a very smooth surface. With penetration from .005 in. to .015 in., the castings still peeled nicely on shake-out, but the surface was slightly rougher, having what now is considered a normal finish for steel castings. As the penetration increased from .015 in. to .025 in., sand started to adhere to the castings on shake-out, but usually could be removed by blasting and scaling during cleaning and heat treatment. When the penetration exceeded .025 in., it was usually necessary to chip or grind this sand metal mixture off the casting. When the penetration exceeded .035 in. it was usually cheaper to scrap the casting than to clean it, unless the penetrated areas were localized and the casting was large. The amount of penetration can easily be measured under the microscope, using a micrometer eyepiece.

21. Although evidence points to penetration as being the major, if not the only cause of adhering sand, at least for steel castings, there can be more than one reason for penetration. A study of the causes of penetration is important, as two of the principal causes are diametrically opposed. We can have penetration due to the opening up of large voids caused by fusion of the sand and, on the other hand, penetration due to the presence of too large voids, caused by too large a grain size in the sand as rammed. As the sintering and fusion points of silica are influenced greatly by grain size, the larger grain-size sand being the more refractory sand, we can have adhering sand due to a too refractory sand. It is necessary to differentiate between these two causes of penetration.

INFLUENCE OF VOIDS IN THE SAND AS RAMMED

22. *Effect of High Temperatures on Voids Present in Sand as Rammed.* All sands in this study of the effect of void size on penetration possessed sufficient flowability to ram without the formation of larger voids due to low flowability. As the effect of flowability on the mold surface is an entirely different subject and this condition was not found in any of the cores used, this phase was not considered.

23. Recent work in subjecting sand to high temperatures in a furnace has shown that the voids present in some sands as rammed,

increase in size as the sand is heated. Other sands crack and create additional voids. This problem is one of dimensional stability and does not come within the scope of this report. It is self-evident that when voids do open up as the result of physical ex-
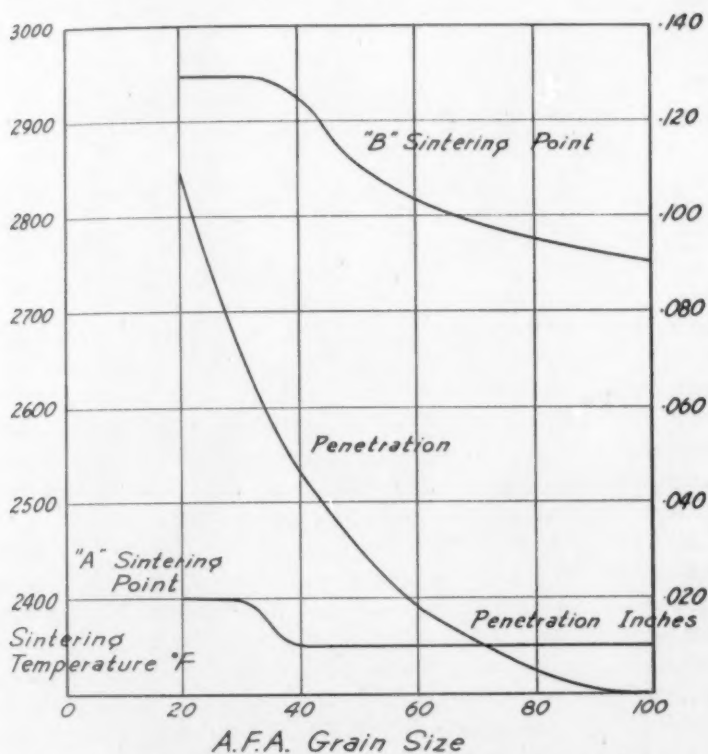
FIG. 8—EFFECT OF GRAIN SIZE ON SINTERING POINTS AND PENETRATION.

pansion when the sand is heated, the final void size must be based on the original void size of the sand as rammed.

24. *Effect of Particle Size and Distribution on Sintering Points and Penetration.* The effect of particle size and distribution on the sintering points and penetration is very pronounced. To check the effect of particle size, a washed and dried silica sand containing 99.8 per cent A.F.A. silica grain and analyzing 98.1 per cent SiO_2 was screened, and the grains caught on each screen were separated. These screen fractions were mixed with a constant bentonite addition of 4.0 per cent and to a constant water content of 4.0 per cent. The effect of grain size on the sintering points and penetration is

Table 1

Effect of Grain Size on Sintering Points and Penetration

4.0 Per Cent Bentonite, 3.0 Per Cent Water Added to all Tests.
Base Sand, 99.8 Per Cent A.F.A. Silica, 0.2 Per Cent A.F.A. Clay.
Chemical Analysis: 98.1 Per Cent SiO_2, 0.9 Per Cent Fe_2O_3, 0.3 Per Cent
Al_2O_3, 0.0 Per Cent CaO, 0.0 Per Cent MgO, 0.0 Per Cent
$K_2O + Na_2O$.

Test Number	Screen Size Through	On	A.F.A. Fineness Number	Green Permeability	Sintering Point Degrees F. "A"	"B"	Penetration inches
A1	20	30	20	280	2400	2950	.108
A2	30	40	30	380	2400	2950	.084
A3	40	50	40	180	2350	2950	.040
A4	50	70	50	150	2350	2850	.024
A5	70	100	70	90	2350	2800	.008
A6	100	140	100	45	2350	2750	.001

shown in Table 1 and plotted in Fig. 8. The effect of grain size on the "B" sintering point is not surprising. Although the "B" point falls from 2950°F. for an A.F.A. 20 mesh sand to 2750°F. for a 100 mesh sand, a number of examples are quoted by Sosman[1] who shows the silica system as being notorious for having definite melting points. The melting points are influenced by a number of variables, one of which is particle size.

25. When these sands are subjected to molten steel, the variation in penetration and adhering sand is shown in Figs. 9 to 14. Figure 9 showing the A.F.A. 20 sand illustrates deep penetration and very bad peel, bad enough to scrap the casting. Penetration decreases with decreasing grain size and void size, even though the refractoriness as measured by the "B" sintering point is also decreased. The finer, less refractory sand has the least penetration and by far the best peel, as shown in Fig. 14. Penetration as measured from these photomicrographs is shown graphically in Fig. 8.

26. It must be emphasized here that in this case we are dealing with a high sintering point sand. Even the lowest "B" sintering point, as shown by the A.F.A. 100 sand, is relatively high, 2750°F. The improvement in peel with decreasing refractoriness is, in this case, due to the elimination of mechanical penetration of the steel into the voids of the sand. The effect of sintering or fusion is inappreciable. If a sand with a lower sintering point is used, the sintering point of the sand may be lowered enough by decreasing the grain size so that an increase in void size due to fusion of the smaller grains becomes pronounced. In this case we would have

an improvement in peel when the grain size is decreased, up to a certain point, then a sharp increase in adhering sand.

27. *Effects of Additions of Silica Flour to Sand.* Another way by which the grain and void size of a molding sand are changed in the foundry is by adding finely divided silica, such as silica flour. When this is done, the problem becomes quite complicated because

Fig. 9a—Sand Surface in Contact with Metal. A.F.A. 20 Sand + 4 Per Cent Bentonite. X25. Oblique Illumination.
Fig. 9b—Section Through Sand-Metal Interface Shown in Fig. 9a. Penetration .110 Inch. X25. Vertical Illumination.

of several other variables. First, the average grain size is not only decreased, but the distribution is changed greatly. The effect of silica flour on the "A" and "B" sintering points is entirely different, as shown in Table 2 and Fig. 15. The "A" point is increased, the "B" point decreased, when increasing amounts of silica flour

FIG. 10a—SAND SURFACE IN CONTACT WITH METAL. A.F.A. 30 SAND + 4 PER CENT BEN-
TONITE. X25. OBLIQUE ILLUMINATION.
FIG. 10b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 10a. PENETRATION
.076 INCH. VERY BAD "BURN-ON." X25. VERTICAL ILLUMINATION.

are added to an A.F.A. 59 silica sand, until at 100 per cent silica flour the ''A'' point is actually higher than the ''B'' point. The reason for the decrease in the ''B'' sintering point, the point of incipient fusion, is, of course, the decrease in particle size as noted previously. The reason for the increase in the ''A'' sintering point, the point when the platinum ribbon starts to stick to the
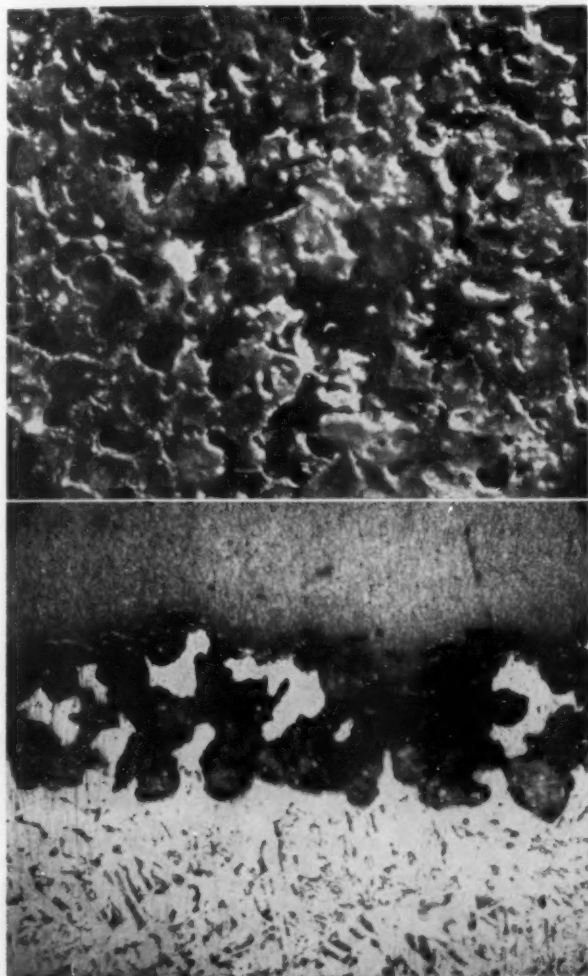
Fig. 11a—Sand Surface in Contact with Metal. A.F.A. 40 Sand + 4 Per Cent Bentonite. X25. Oblique Illumination.

Fig. 11b—Section Through Sand-Metal Interface Shown in Fig. 11a. Penetration .040 Inch. Bad ''Burn-On.'' X25. Vertical Illumination.

sand specimen, is obscure, but probably is due in part to the great
increase in hot and retained strength of a sand mix with large silica
flour additions. Then, the "B" sintering point of the very fine silica
flour is so much lower than that of the coarse particles of sand to
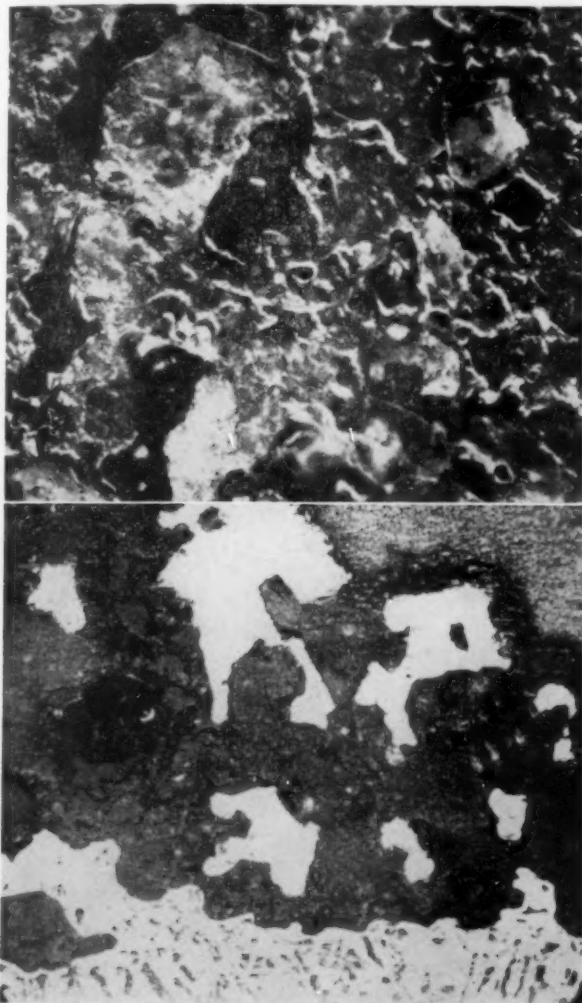which it is added, that the condition mentioned before of a com-

FIG. 12a—SAND SURFACE IN CONTACT WITH METAL. A.F.A. 50 SAND + 4 PER CENT BEN-
TONITE. X25. OBLIQUE ILLUMINATION.
FIG. 12b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 12a. PENETRATION
.024 INCH. SURFACE ROUGH WITH SOME ADHERING SAND. X25. VERTICAL ILLUMINATION.

bination of decreased mechanical penetration together with in-
creased penetration due to fusion of the smaller grains of silica
flour is probable. With this type of mix, part coarse and part
very fine, two other variables for which the silica system is notori-
ous must be taken into consideration; namely, the high viscosity of
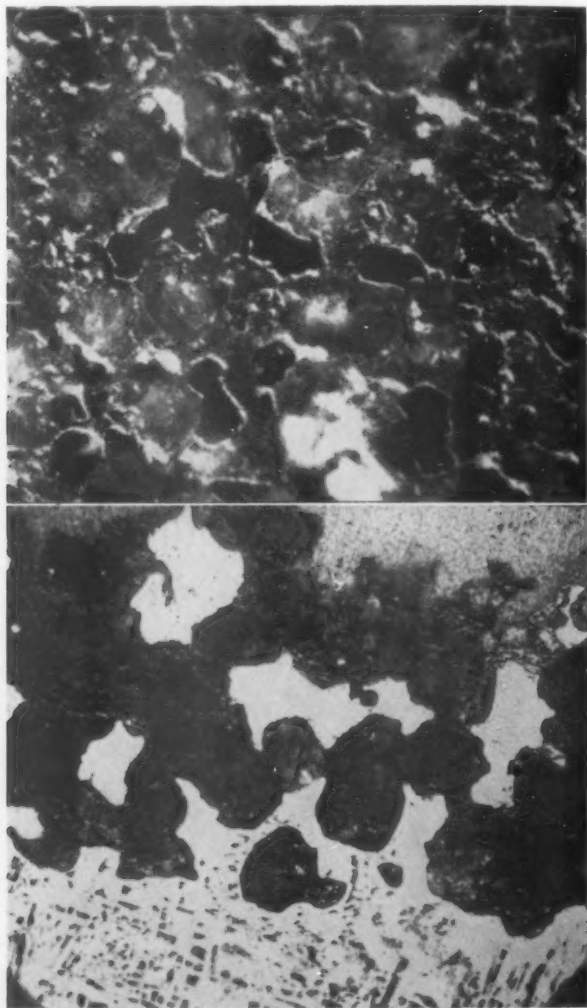the liquid phase and the extreme sluggishness of the system. If
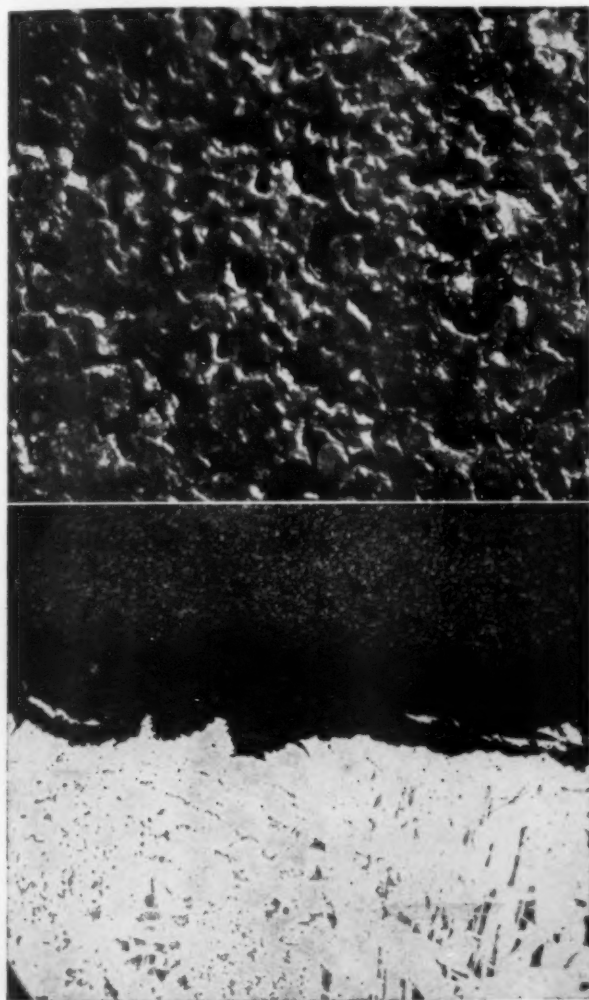
FIG. 13a—SAND SURFACE IN CONTACT WITH METAL. A.F.A. 70 SAND + 4 PER CENT BEN-
TONITE. X25. OBLIQUE ILLUMINATION.
FIG. 13b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 13a. PENETRATION
.008 INCH. SURFACE SMOOTH. X25. VERTICAL ILLUMINATION.

the system is at all heterogeneous, hours—not minutes or seconds—
are required to reach equilibrium. This point is important as in
the foundry, except for very massive castings, we are not dealing
with equilibrium conditions but with non-equilibrium conditions.
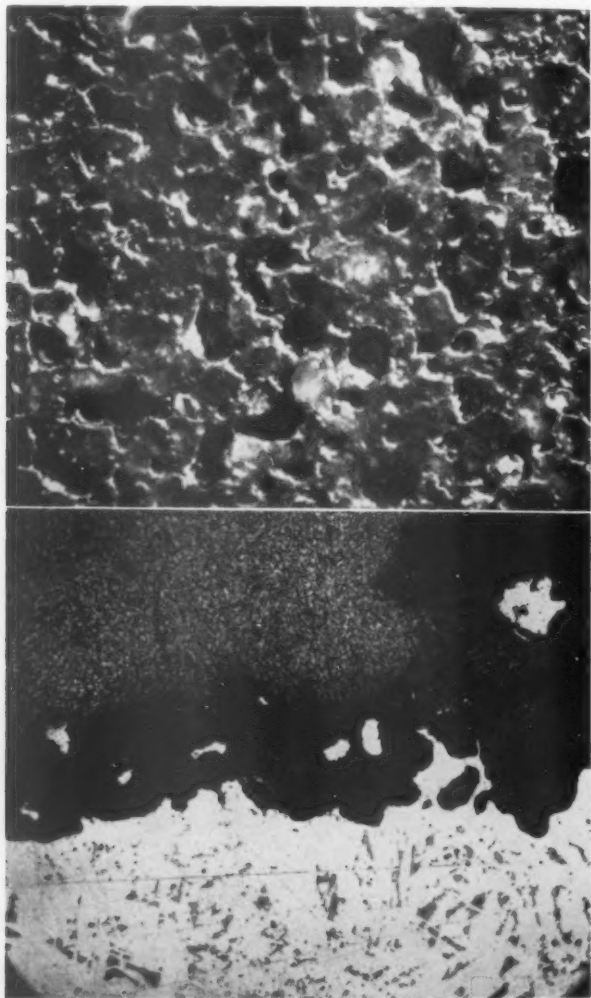All of these variables must be taken into consideration when study-
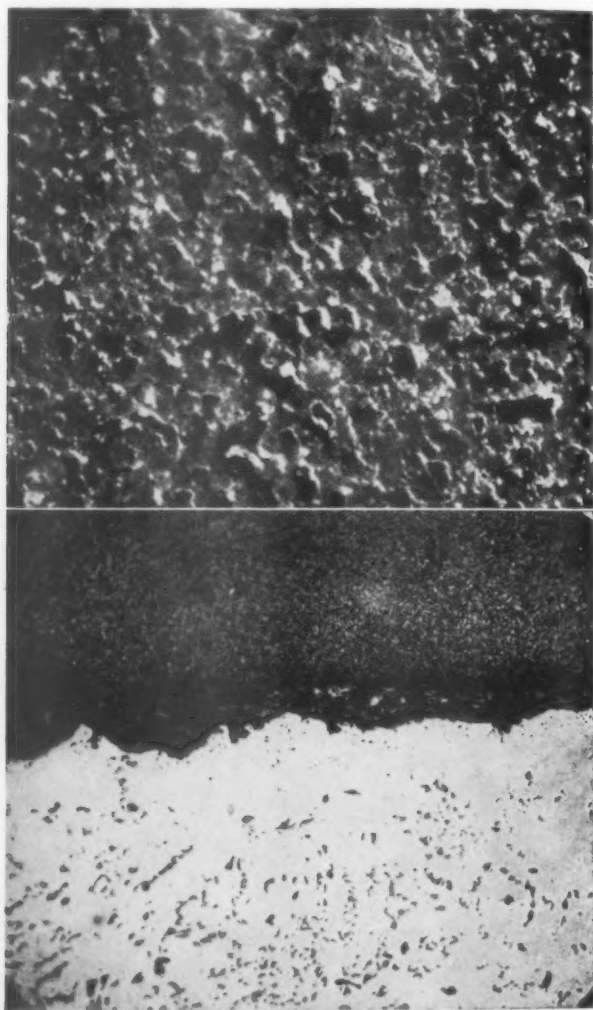
FIG. 14a—SAND SURFACE IN CONTACT WITH METAL. A.F.A. 100 SAND + 4 PER CENT BEN-
TONITE. X25. OBLIQUE ILLUMINATION.
FIG. 14b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 14a. NO PENETRA-
TION. X25. VERTICAL ILLUMINATION.

ing the addition of silica flour to molding sand, and are the answer to why different results are obtained by different foundries with silica flour under what seem to be the same conditions.
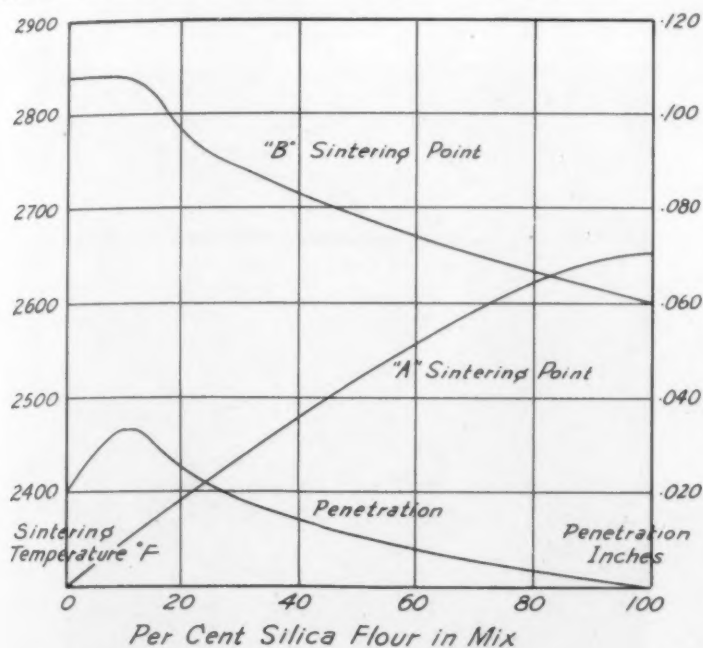
FIG. 15—EFFECT OF SILICA FLOUR ON SINTERING POINTS AND PENETRATION.

Table 2

EFFECT OF SILICA FLOUR ADDITIONS ON SINTERING POINTS AND
PENETRATION

Base Sand, 99.8 Per Cent A.F.A. Silica, 0.2 Per Cent A.F.A. Clay.
A.F.A. Fineness No. 59.
Minus 140 Mesh Silica Flour. 90 Per Cent minus 325 Mesh.
Chemical Analysis of Sand and Silica Flour, Same as Table 1.
4.0 Per Cent Bentonite Added to all Mixes. Water Added to Temper.

Test Number	Per Cent A.F.A. Number 59 Sand	Per Cent Silica Flour	A.F.A. Fineness Number of Mix	Per Cent Water	Green Permeability	Sintering Point Degrees F. "A"	"B"	Penetration-inches
A7	100	0	59	3.25	160	2300	2850	.020
A8	95	5	66	3.60	105	2350	2850	.024
A9	90	10	73	3.40	90	2400	2850	.036
A10	75	25	94	5.71	10	2400	2750	.022
A11	50	50	129	7.20	0	2550	2700	.008
A12	0	100	250	10.60	0	2650	2600	.001

28.   The effect of adding increasing amounts of silica flour to
an A.F.A. 59 sand is plotted in Fig. 15. In this series we do not
have a gradual decrease in penetration and sand adherence with
a decrease in the average fineness of the sand as we did when the

FIG. 16a—SAND SURFACE IN CONTACT WITH METAL. SILICA SAND, 4 PER CENT BENTON-
ITE.   NO SILICA FLOUR.   X25.   OBLIQUE ILLUMINATION.
FIG. 16b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 16a.   PENETRATION
.016 INCH.   SURFACE FAIRLY SMOOTH.   X25.   VERTICAL ILLUMINATION.

grain size was decreased uniformly, but first an increase, then a decrease. This point has been checked repeatedly in a number of different ways. Maximum penetration and sand adherence occur when between 10 and 25 per cent silica flour is in the mix. The sand surfaces and sand-metal interface of the 0, 10 and 100 per
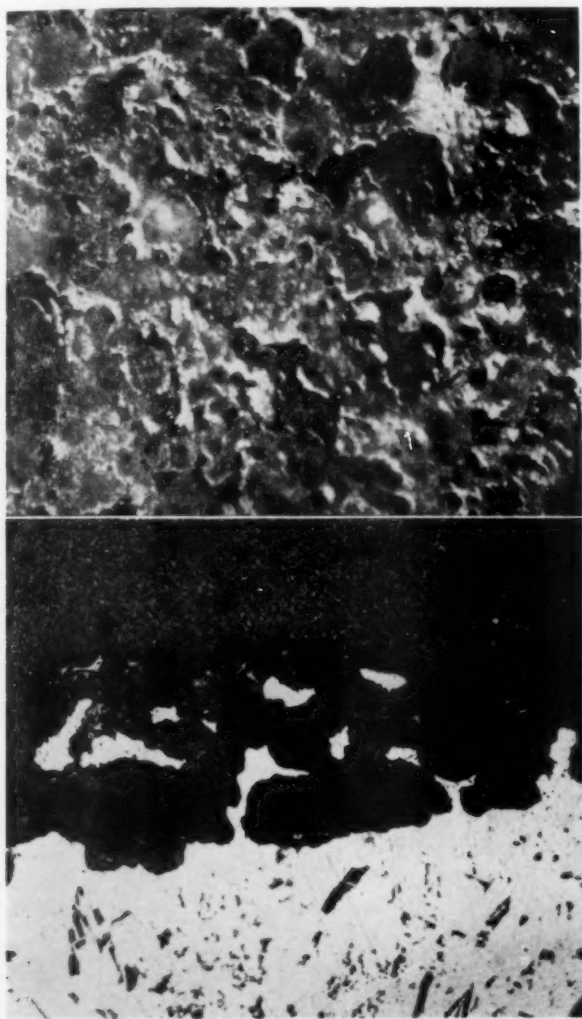
FIG. 17a—SAND SURFACE IN CONTACT WITH METAL. SILICA SAND, 4 PER CENT BENTONITE, 10 PER CENT SILICA FLOUR. X25. OBLIQUE ILLUMINATION.
FIG. 17b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 17a. PENETRATION .036 INCH. SURFACE ROUGH WITH ADHERING SAND. X25. VERTICAL ILLUMINATION.

cent silica flour mixes are shown in Figs. 16 to 18 inclusive.

29. This behavior can only be explained by keeping all the variables previously mentioned in mind. The sand containing no silica flour and shown in Fig. 16 is fairly uniform, with an average A.F.A. grain size of 59. When 5 per cent silica flour is added, there is very little difference in penetration. The increase in pene-

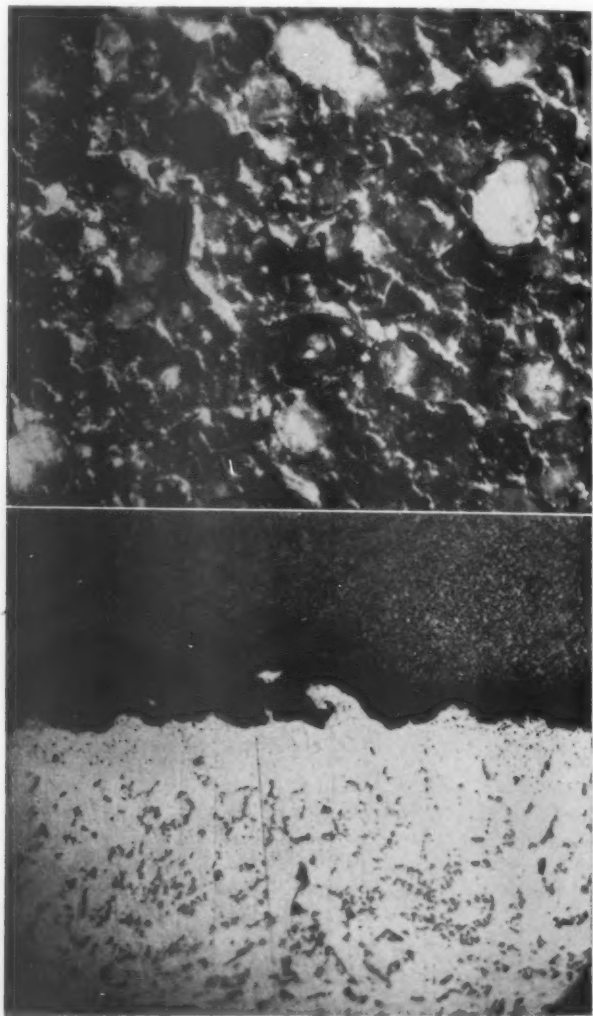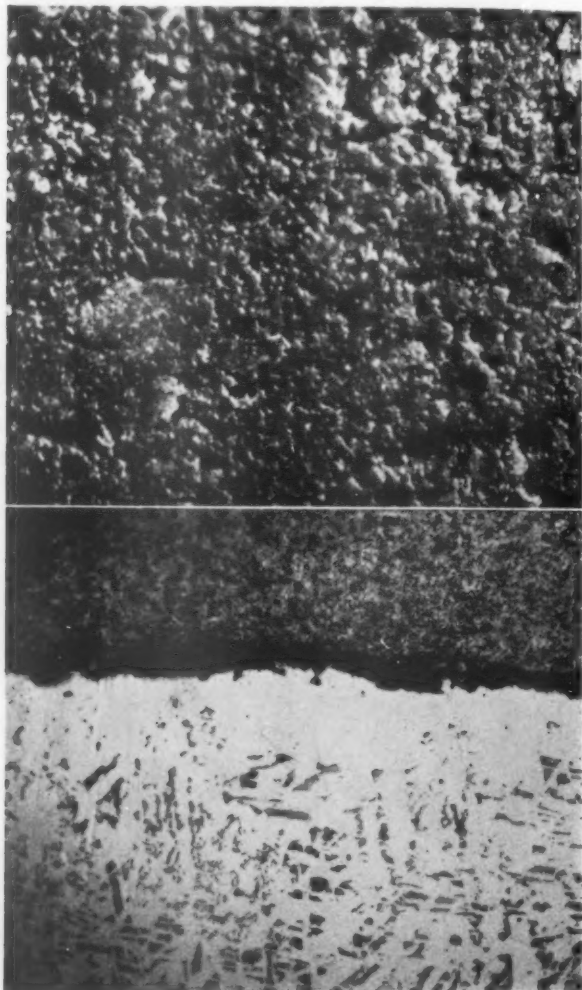FIG. 18a—SAND SURFACE IN CONTACT WITH METAL. 100 PER CENT SILICA FLOUR, 4 PER CENT BENTONITE. X25. OBLIQUE ILLUMINATION.

FIG. 18b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 18a. NO PENETRATION. SURFACE VERY SMOOTH. X25. VERTICAL ILLUMINATION.

tration reaches a maximum at 10 per cent silica flour (Fig. 17). Within this range of 10 to 25 per cent silica flour we have the maximum variation in distribution, the sand consisting of the larger grains of the base sand, with appreciable space being taken up by the fine grains of the silica flour that start to fuse 250°F. lower than the larger grains of the base sand. The viscosity of silica just above its melting point has not been determined but is known to be very high.

30. At a constant temperature, as determined by the pouring temperature of the metal and the rate of heat flow through the
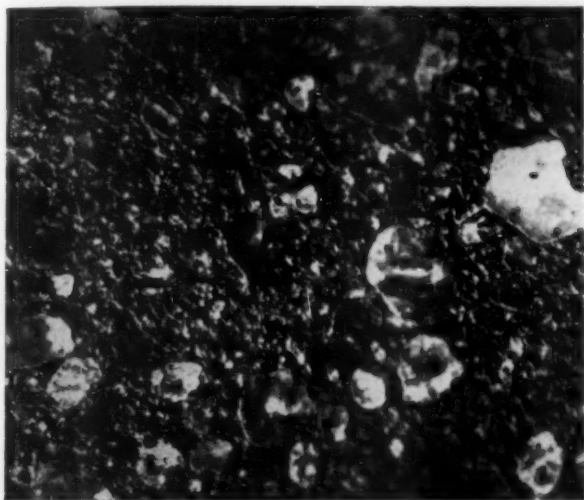
Fig. 19—Silica Wash Not Completely Covering Larger Grains of Base Sand. Surface as Washed. X25. Oblique Illumination.

sand, the smaller grains of the silica flour are the first to fuse. The larger grains of the base sand are still solid and can act as nuclei. The high viscosity and surface tension of the molten silica pulls this constituent around the solid larger grains, opening up voids in the space originally occupied by the silica flour. With low percentages of silica flour these voids are not appreciable because the space taken up by the silica flour is not appreciable. When the sand contains 10 to 25 per cent silica flour, the space occupied by the silica flour does constitute an appreciable part of the whole mass and large enough voids can be opened up to allow the metal to penetrate the sand.

31. With higher percentages of silica flour approaching 50

FIG. 20—PENETRATION IN BACK OF WASH DUE TO CONDITION SHOWN IN FIG. 19. X25.
VERTICAL ILLUMINATION.
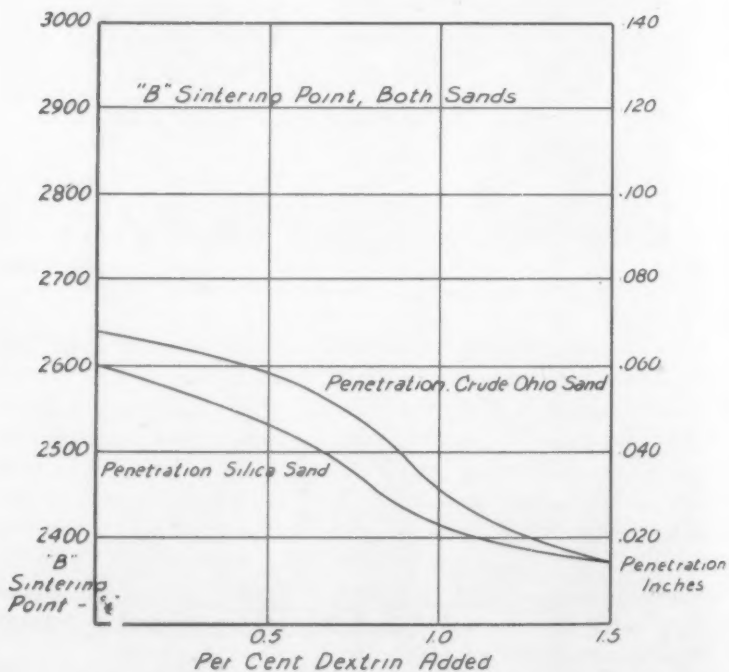
FIG. 21—EFFECT OF DEXTRIN ON "B" SINTERING POINT AND PENETRATION.

Table 3

EFFECT OF DEXTRIN ON SINTERING POINTS AND PENETRATION

Test Number	Type of Sand	A.F.A. Fineness Number	Per Cent Bentonite Added	Per Cent Clay Added	Per Cent Dextrin Added	Per Cent Water	Green Permeability	Sintering Point Degrees F. "A"	"B"	Penetration inches
H1	Silica	55	—	8.0	0.0	4.06	110	—	2850	.060
H2	Silica	55	—	8.0	0.5	3.10	125	—	2850	.050
H3	Silica	55	—	8.0	1.0	3.10	140	—	2850	.018
H4	Silica	55	—	8.0	1.5	3.10	160	—	2850	.016
H5	Crude Ohio	55	2.0	—	0.0	3.06	160	2350	2875	.066
H6	Crude Ohio	55	2.0	—	0.5	3.20	155	2400	2850	.060
H7	Crude Ohio	55	2.0	—	1.0	3.20	165	2350	2850	.024
H8	Crude Ohio	55	2.0	—	1.5	3.20	165	2350	2850	.014

per cent, the relative number of large grains that form nuclei decreases, and although there is more fusion at the surface in contact with the steel, as shown in Fig. 18, this fusion is uniform with sort of a glazing action, no penetration, and consequently good peel. Even though these sands have been heated farther above their melting points, the liquid silica formed is continuous and viscous enough to hold back the metal. The high surface tension that must accompany this high viscosity has no discontinuities on which to act, and we have the formation of a very viscous film. This action can be compared to stretching a thin rubber balloon. It can be stretched tremendously and still retain the property of confining a liquid even though it be stretched to no more than a thin continuous film. However, the moment it comes in contact with a solid sharp object, watch the voids open up!

32. *Effect of Silica Flour Used as a Wash.* Exactly the same conditions exist when silica flour is used as a wash. If the silica flour wash is applied properly, we have a uniform coating of fine grains of silica which, although not particularly refractory, fuse uniformly, do not open up voids, and resist the penetration of the molten metal into the larger voids of the base sand. If, however, the wash is not applied properly and some of the larger grains of the base sand are also exposed to the metal as shown in Fig. 19, voids will open up in these areas, allowing the metal to penetrate and permeate the voids of the coarser sand in back of the wash, causing sand adherence, as shown in Fig. 20.

33. Of course, if the breakdown of the silica flour discussed in the preceding paragraphs is to occur, the temperature of the metal must be high enough to not only fuse the fine silica flour particles, but also to heat the sand to above the melting point of the metal itself. In the cases given, both these conditions have been met. The recurrent trouble with silica flour in the foundry is evidence that these conditions are met in actual practice. If the pouring temperature of the metal is not high enough to melt the fine particles of the silica flour, no sand will adhere to the casting. If the pouring temperature of the metal is high enough to cause fusion but, for any reason, heat flow through the sand is high enough to prevent the sand being heated above the melting point of the metal, penetration again will not occur, as the metal solidifies before it can penetrate the voids that have opened up. This is the reason that such bad "burn-on" is found at reentrant angles and pockets of castings poured in molds made with silica flour, when all other surfaces peel perfectly.

34. The fact that a properly applied wash is composed of a coating of uniform fine grains explains why even properly washed molds break down suddenly if the temperature of the metal entering the molds is increased slightly. If the temperature is too high, the whole mass fuses as a unit with deep penetration and very poor peel resulting. The larger grains of the usual molding sands not only fuse at a higher temperature, but over a range of temperatures, acting as buffers. The increase in penetration with increasing temperature of sand is then more gradual. When molds are washed with silica flour, an increase in temperature of less than 50°F. can mean the difference between perfect peel and very bad "burn-on."

EFFECT OF ORGANIC BINDERS ON PENETRATION

35. Surprisingly enough, organic binders influence the amount of penetration and adhering sand to a remarkable degree, although they have no effect on either the "A" or "B" sintering points. Different types vary greatly in their effects. Table 3 and Fig. 21 show the effect of dextrin. The addition of 1.5 per cent dextrin decreases the penetration from .060 inches to .018 inches in this particular test. As .060 inches means bad "burn-on" while .018 inches will give fair peel, the addition of cereal binders can mean the difference between good peel and trouble with adhering sand in the cleaning room. This point has been checked repeatedly in practice.

36. *Effect of Pouring Temperature.* The effect of pouring temperature is also shown very clearly in this series. The temperature of the metal entering the mold in this series was 3060°F., at least 40°F. higher than the rest of the blocks. As can be seen by comparing the penetration in H1 and H5 (Table 3), with B4 and B7 (Table 5), the same sands cast in different blocks at 3000°F., this increase in pouring temperature of 60°F. more than doubles the penetration with a corresponding increase in adhering sand.

37. *Explanation of Action of Organic Binders.* Two reasons can be advanced to explain the action of organic binders. As there is no difference in the sintering points or fusion of the sand in contact with the metal, fusion or sintering must be eliminated in this instance. One possible explanation is that the cereal binders create a reducing atmosphere at the sand-metal interface, preventing penetration by the formation of "over-reduced steel," which does not penetrate the sand because of its low fluidity.

38.   The other theory is simply one of gas pressure preventing metal penetration. The large evolution of gas in a sand containing cereal binders has been shown by Briggs and Morey[9]. Although the pressure developed has not been measured, it should be considerable.

39.   Without further study of the reason for the action of cereal binders on penetration, one cannot say which of these two theories is correct or, in fact, that there is not some other reason for this action. Nevertheless, the decrease in penetration has been checked repeatedly and enough data have been collected to show that all organic binders do not have the same effect. It seems that nearly all types of cereal additions will have about the same effect as dextrin on penetration. Core oil, when added in the same amounts, has a slightly smaller effect. On the other hand, the pitch and gilsonite-base additions will not decrease penetration at all; if anything, they increase penetration slightly.

EFFECT OF IMPURITIES IN THE SAND

40.   In the previous part of this report we have been dealing with relatively pure silica sands. Another phase equally as important is the formation of large voids, due to fusion because of impurities present in the sand, and lowered refractoriness due to these impurities, resulting in penetration and adhering sand. If we could use pure silica in the foundry, this problem would never occur, but other materials must be added to silica to make it moldable. Regardless of what is added, even though the fusion point of the addition is higher than that of pure silica, the mixture will have a lower fusion point than that of either of the components. The only proviso here is that the system reach equilibrium. Even though equilibrium is not attained in the foundry because of the sluggishness of the silica system, equilibrium conditions must be determined before the more important non-equilibrium conditions can be determined. There seems to be no information in the literature on this point.

41.   Too often one hears the fusion or sintering point of an addition to molding sand quoted as a criterion of its behavior in the foundry. Nothing could be more misleading. The mixture of a sand and a very refractory material can, as often as not, have a lower melting and sintering point than a comparable mixture of a sand and another material with a much lower melting and sinter-

Table 4

CHEMICAL ANALYSIS OF SANDS AND ADDITIONS

Materials	A.F.A. Fineness Number	Per Cent A.F.A. Silica	Per Cent A.F.A. Clay	A.F.A. Silica Fraction							A.F.A. Clay Fraction						
				SiO_2	Fe_2O_3	Al_2O_3	CaO	MgO	K_2O plus Na_2O	Ignition Loss	SiO_2	Fe_2O_3	Al_2O_3	CaO	MgO	K_2O plus Na_2O	Ignition Loss
Silica Sand "B"	59	99.8	0.2	98.1	0.9	0.3	0.0	0.0	0.0	0.0	Not enough to analyze						
Crude Ohio Sand "C"	51	96.6	3.4	97.1	0.4	0.0	0.3	0.3	0.0	0.0	47.3	5.4	30.0	0.6	1.5	0.0	11.0
Kaolin-Type Fire Clay	—	—	—	—	—	—	—	—	—	—	54.8	1.1	27.3	0.3	0.3	2.0	10.2
Western Bentonite	—	—	—	—	—	—	—	—	—	—	61.5	2.3	22.2	0.0	1.5	3.0	11.0
Lime ($CaCO_3$)	—	—	—	—	—	—	—	—	—	—	—	—	—	96.0	—	—	—
Dolomite	—	—	—	—	—	—	—	—	—	—	—	—	—	47.8	33.8	—	—
Iron Oxide (Fe_2O_3)	—	—	—	—	—	—	—	—	—	—	—	98.0	—	—	—	—	—
Alumina (Al_2O_3)	—	—	—	—	—	—	—	—	—	—	—	—	90.0	—	—	—	—
Loam	—	—	—	—	—	—	—	—	—	—	80.1	6.7	14.3	0.0	0.0	0.0	—
Salt—C. P. NaCl																	
Borax—C. P. $Na_2B_4O_7$																	

ing point. The lime-silica system is just one of many that can be
used as an illustration.

42. In order to obtain quantitative data on the effect of the
elements either present in the base sand or added to the sand in
the foundry, a number of mixes were made up with increasing
amounts of these elements. The sintering points were determined
and the behavior of the mixes when in contact with molten steel
studied.

43. *Methods of Obtaining Data on the Effects of Impurities.*
Data were collected on the sintering points and behavior of both
western bentonite and kaolin-type fire clay when added in amounts
from 0 to 25 per cent to both pure silica sand and to a crude Ohio
sand. The same information was obtained for lime ($CaCO_3$),
dolomite ($CaO + MgO$), iron oxide (Fe_2O_3), alumina (Al_2O_3),
and also a loam selected as representative of the colloids present
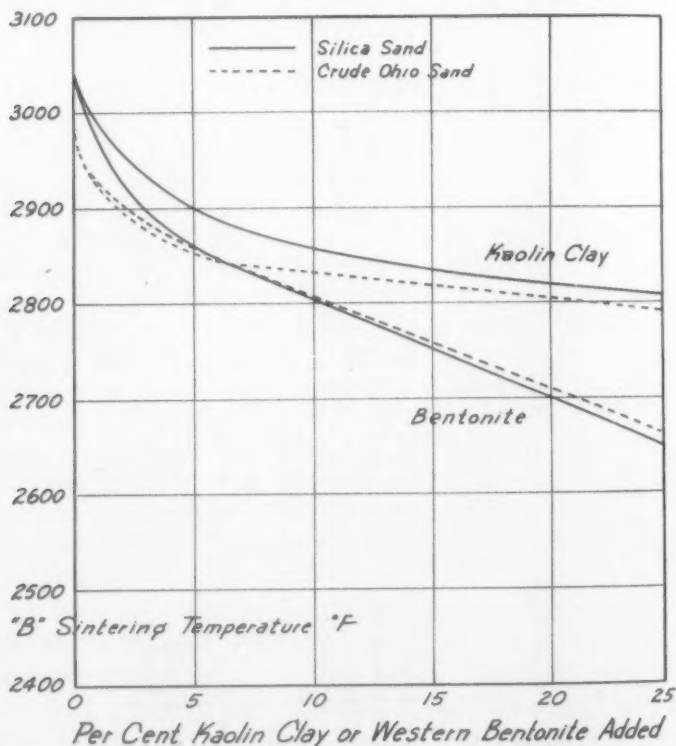in crude molding sand. These elements were selected as representa-

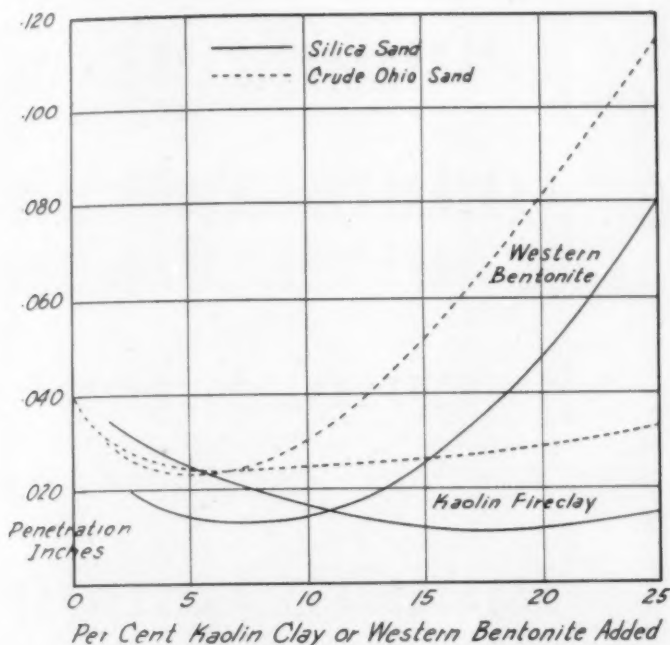FIG. 22—EFFECT OF CLAY AND BENTONITE ON "B" SINTERING POINT.

FIG. 23—EFFECT OF CLAY AND BENTONITE ON PENETRATION.

tive of the impurities present in crude molding sands. The action of salt (NaCl) and borax ($Na_2B_4O_7$) was also studied, not only to include some very strong fluxes, but also to see whether it would be possible to obtain a glaze similar to that used on pottery on the sand surface, which would act as sort of an automatic wash by melting and filling up the voids and stopping penetration.

44. The chemical analyses of the sands and the additions are given in Table 4. The sands were washed into the A.F.A. silica and A.F.A. clay fractions before analyzing, and the analysis of the two fractions was reported separately. It should be emphasized at this point that all additions were made as a fine powder, in solution in water or as a slurry, so as to obtain uniform distribution on the silica grains. They were then present in the A.F.A. clay fraction and were present in the sand mix as a separate phase, surrounding the silica phase, the silica grains. This is important, as will be shown later.

45. *Effect of Additions of Kaolin Clay and Western Bentonite to a Crude Ohio Sand.* The effect on the sintering points and

Table 5

EFFECT OF KAOLIN FIRE CLAY AND WESTERN BENTONITE ON SINTERING POINTS AND PENETRATION

Test No	Type of Base Sand	Type of Addition	Per Cent Added	Per Cent Water	Green Permeability	Sintering Point Degrees F. "A"	"B"	Penetration-inches
B1	Silica	Fire Clay	0.0	3.40	130	—	3050	—
B2	Silica	Fire Clay	2.0	3.00	150	—	2900	.025
B3	Silica	Fire Clay	4.0	3.65	140	—	2900	.030
B4	Silica	Fire Clay	8.0	4.06	110	2350	2850	.020
B5	Silica	Fire Clay	15.0	5.22	50	2400	2850	.012
B6	Silica	Fire Clay	25.0	7.60	20	2400	2800	.015
B7	Silica	Bentonite	2.0	3.30	160	2600	2950	.020
B8	Silica	Bentonite	4.0	3.90	140	2500	2900	.015
B9	Silica	Bentonite	8.0	4.18	130	2400	2800	.017
B10	Silica	Bentonite	15.0	5.68	130	2350	2750	.020
B11	Silica	Bentonite	25.0	8.15	125	2300	2650	.080
C1	Crude Ohio	Fire Clay	0.0	5.65	230	2600	3000	.040
C2	Crude Ohio	Fire Clay	2.0	3.00	165	2400	2850	.030
C3	Crude Ohio	Fire Clay	4.0	3.61	145	2400	2850	.025
C4	Crude Ohio	Fire Clay	8.0	4.00	105	2400	2825	.025
C5	Crude Ohio	Fire Clay	15.0	5.96	55	2300	2825	.025
C6	Crude Ohio	Fire Clay	25.0	7.43	10	2300	2775	.030
C7	Crude Ohio	Bentonite	2.0	3.06	160	2350	2875	.020
C8	Crude Ohio	Bentonite	4.0	3.91	150	2350	2850	.020
C9	Crude Ohio	Bentonite	8.0	4.89	145	2300	2825	.025
C10	Crude Ohio	Bentonite	15.0	5.90	160	2300	2775	.050
C11	Crude Ohio	Bentonite	25.0	8.56	200	2200	2650	.110

penetration of kaolin clay and western bentonite additions to a crude Ohio sand and to a pure silica sand are shown in Table 5 and Figs. 22 and 23. As shown in Fig. 22, the addition of kaolin clay lowered the "B" sintering point less than did western bentonite. The influence of the base sand is interesting. The natural
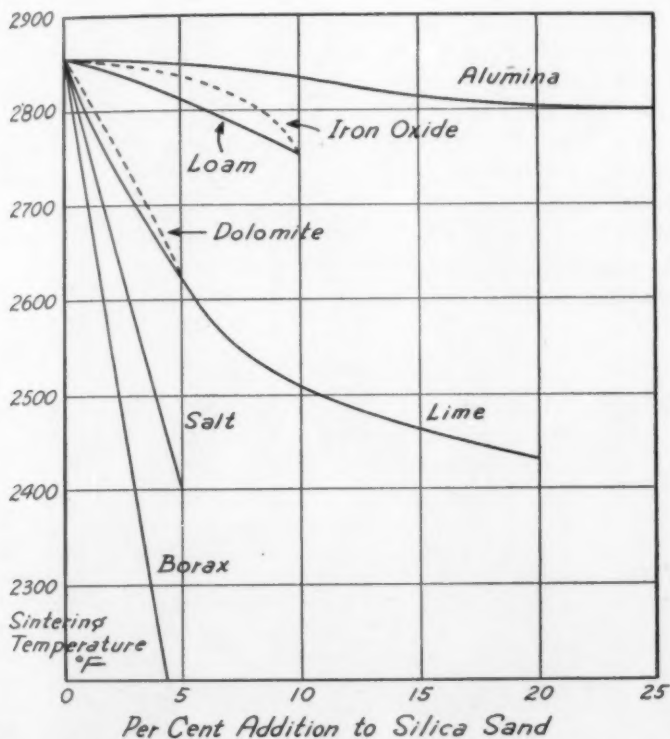
Fig. 24—Effect of Impurities in the A.F.A. Clay Fraction on the "B" Sintering Point.

colloids present in the crude sand lowered the sintering point, with the same clay or bentonite addition, when the clay or bentonite additions were low. With higher contents, the effect of the addition overshadowed the effect of the natural colloids present in the crude sand and the sintering points of both sands were about the same with the additions.

46. In actual practice, however, the effect of fire clay, bentonite, silica or crude sand is not as great as one would presume from this data. In the steel foundry, clay-type additions are usually made

for green strength. Less bentonite is required for a given green strength than fire clay, and less of either is required with a natural sand than with a pure silica sand. The sand mixes in any foundry, then, will usually show about the same sintering points, regardless of what combination is used.

47.   As shown in Fig. 23, the shapes of the penetration curves for bentonite and fire clay are entirely different than those previously shown for silica flour. Instead of an increase in penetration followed by then a decrease, there is a decrease followed by an increase. The reason for this can be seen by comparing the "B" sintering point curves. Small additions of bentonite and fire clay decrease the "B" sintering point sharply. A slight, high-silica glaze is formed, decreasing the penetration slightly. As this semiliquid silicate coats the silica grains and is not present in discrete areas as was silica flour, no voids open up.

48.   With larger additions of fire clay or bentonite, the liquid

Table 6

EFFECT OF IMPURITIES IN THE A.F.A. CLAY FRACTION ON THE SINTERING POINTS AND PENETRATION. ADDITION, WITH 4.0 PER CENT BENTONITE, ADDED TO SILICA SAND B

Test No.	Addition to A.F.A. Clay Fraction	Per Cent Added	Per Cent Water	Green Permeability	Sintering Point Degrees F. "A"	"B"	Penetration-inches
D1	Lime	0.0	3.00	160	2300	2825	.014
D2	Lime	10.0	6.00	80	2200	2500	.014
D3	Lime	20.0	6.20	45	2250	2425	.052
E1	Dolomite	5.0	4.00	105	—	2625	.020
E2	Loam	3.0	3.00	135	2300	2850	.024
E3	Loam	5.0	3.90	125	2300	2800	.022
E4	Loam	10.0	5.00	80	2200	2750	.018
F1	Iron Oxide	0.0	3.25	160	2300	2825	.031
F2	Iron Oxide	0.5	3.00	155	2350	2825	.022
F3	Iron Oxide	2.0	3.00	160	2350	2800	.031
F4	Iron Oxide	5.0	2.90	150	2400	2825	.035
G1	Iron Oxide	10.0	3.75	—	2400	2750	.050
F5	Alumina	0.5	2.70	130	2400	2850	.022
F6	Alumina	1.0	2.70	130	2300	2825	.016
F7	Alumina	5.0	2.85	120	2400	2850	.026
F8	Alumina	25.0	5.00	55	2350	2800	.040
F9	Salt	5.0	5.85	120	1800	2400	.001
F10	Borax	1.0	3.00	155	2200	2750	.024
F11	Borax	2.5	3.50	145	1800	2500	.060
F12	Borax	5.0	4.70	115	1650	2200	.052

phase not only increases in volume, but is now a more impure silicate with much lower viscosity. As the clay or bentonite is still coating the silica grains solid nuclei are present. The clay or bentonite will reach equilibrium first with the smaller grains of silica, again forming viscous high-silica silicates; these liquid silicates
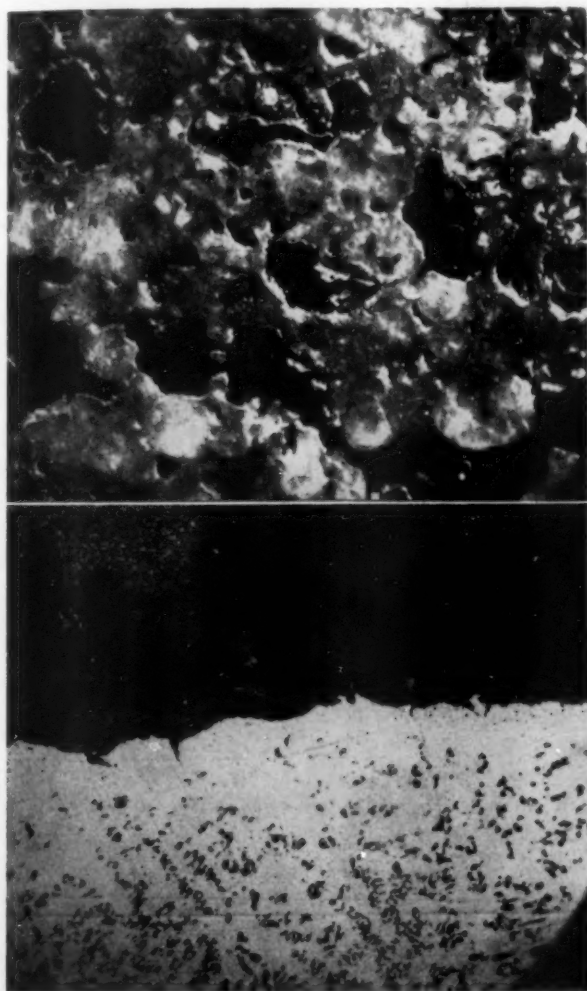

Fig. 25a—Sand Surface in Contact with Metal. Silica Sand, 4 Per Cent Bentonite, 5 Per Cent Salt. X25. Oblique Illumination.
Fig. 25b—Section Through Sand-Metal Interface Shown in Fig. 25a. No Penetration. Surface Very Smooth. X25. Vertical Illumination.

formed will draw around the larger solid silica grains, forming larger voids than present originally.

49.   The increase in penetration, as shown by the slope of the lines in Fig. 23, is governed by the sintering point of the mix. As shown in Fig. 22, fire clay decreases the sintering point much less than bentonite. The penetration with clay, as shown in Fig. 23, decreases at first as the voids are filled up with a viscous glaze, and then increases only slightly up to 25 per cent fire clay in the mix. When the western bentonite is increased over 5 per cent in crude sand, penetration increases very rapidly. A 10 per cent addition to pure silica sand is required before penetration and adhering sand are encountered. The reasons for this behavior can be seen from the positions of the sintering point curves in Fig. 22.

50.   As is to be expected, the effect of impurities present in the A.F.A. clay fraction is to decrease the sintering point and, in most instances, increase penetration and adhering sand, as shown in Table 6 and Fig. 24. Their effect on penetration and adhering sand in all but one instance follows the sintering point curve. Not until the "B" sintering point has been lowered to below 2775°F. do we see any appreciable increase in fusion or any increase in void size and penetration due to this fusion. Iron oxide, alumina and loam decrease the "B" sintering point so little that in the range encountered in practically all sands used in the foundry, they can be said to have no effect on penetration. In fact, it has been shown in practice that iron oxide will decrease penetration and adhering sand.

51.   *Effect of Dolomite and Lime in the Clay Fraction of Sand.* Dolomite and lime, when present in the A.F.A. clay phase, lower the "B" sintering point to a greater degree, and consequently do increase penetration when present in sufficient amounts. However, much larger amounts are required than are encountered in the foundry except in unusual circumstances. Ten per cent lime and 5 per cent dolomite must be present in the A.F.A. clay fraction before the "B" sintering point is lowered enough to cause appreciable fusion resulting in penetration and adhering sand.

52.   As would be expected, the addition of sodium salts decreases the "B" sintering point greatly. In the case of borax, penetration and adhering sand are encountered with small additions. The addition of slightly over 1 per cent borax is sufficient to cause deep penetration and bad "burn-on," as shown in Table 6. Salt, on the other hand, in amounts up to 5 per cent actually decreases penetration from .020 inches to .001 inches, even though

almost complete fusion occurs and large voids open up in the sand as shown in Fig. 25. The only explanation that can be given for this surprising action is gas pressure. Salt volatilizes much below the pouring temperature of the metal, and the action here may be the same as that of the cereal binders.

53. The same effect is found when a saturated solution of salt is sprayed on the surface of the sand in contact with the metal—almost complete fusion, but no penetration and very good peel. This last point may have a practical application. The addition of salt to the sand itself is impractical because of the serious corrosion problem that would be encountered in the sand handling equipment. However, salt added to the wash, if used, is an old foundry practice that has fallen into disuse in recent years. It might be worth while to revive this practice.

EFFECT OF IMPURITIES IN THE SILICA PHASE

54. Although the addition of impurities to the A.F.A. clay phase studied in the previous part of this report does lower the sintering points and causes penetration, a surprisingly large addition is required to cause trouble in the foundry. This is at variance with the trend in steel foundry practice involving synthetic sands, as the trend for years has been toward purer and purer silica as the base sand, bonding with the least amount of binder to make the sand just moldable. Then too, there have been instances when

Table 7

PHYSICAL AND CHEMICAL PROPERTIES OF SANDS WITH IMPURE A.F.A. SILICA

Test No.	Per Cent A.F.A. Silica	Per Cent A.F.A. Clay	A.F.A. Fineness Number	A.F.A. Silica Fraction						
				SiO_2	Al_2O_3	Fe_2O_3	CaO	MgO	Na_2O+K_2O	Loss on Ignition
J6	96.8	3.2	56	46.0	7.1	2.9	16.9	5.1	3.5	17.7
K6	99.8	0.2	38	76.2	7.6	2.3	2.5	0.2	10.8	1.5
J7	73.0	27.0	60	85.2	4.6	2.5	0.0	0.0	5.6	1.0
J8	64.3	35.7	47	73.3	7.9	2.6	3.1	2.7	3.4	3.5
J9	80.7	19.3	135	97.0	1.3	0.5	0.0	0.0	0.0	0.4

	A.F.A. Clay Fraction					Na_2O+K_2O	Loss on Ignition	Sintering Point Degree F.		Penetration-inches
	SiO_2	Al_2O_3	Fe_2O_3	CaO	MgO			"A"	"B"	
J6	—	—	—	—	—	—	—	2200	2300	.25
K6	—	—	—	—	—	—	—	2000	2275	.25
J7	51.7	22.1	7.5	0.0	0.0	0.7	8.6	2250	2500	.12
J8	53.9	17.9	9.6	2.4	2.3	4.0	8.5	2200	2300	.25
J9	46.9	29.7	5.0	0.0	0.0	1.9	0.9	2350	2675	.014

little more than traces of impurities in certain sands have rendered them unfit for steel foundry use.

55.   The sands studied in the previous section represent only one phase of the effect of impurities in molding sands. All the impurities were present in the A.F.A. clay fraction and existed in the mix as a separate phase, coating the silica phase, the sand grains. Another aspect of the subject that must be taken into consideration is when the impurities are present in the A.F.A. silica fraction. In this case, the impurities are present as silicates. This is important when one considers again the sluggishness of the

FIG. 26—SURFACE OF SAND J6 IN CONTACT WITH METAL. COMPLETE FUSION. PENETRATION .25 INCH.   X1.5.

silica system. When the sand mix consists of two phases, the time element is much too long for equilibrium even to be approached in the foundry, with the possible exception of when the sand is in contact with very heavy castings weighing tons. When the impurities are present in the silica fraction as silicates, equilibrium was established ages ago when the sand was formed, and we are now dealing with equilibrium conditions. Any low melting silicates in the sand will melt at the point designated in the equilibrium diagram within the time element encountered in the foundry.

56.   *Description of Sands Used to Determine the Effects of Impurities in the Silica Fraction.* The results of studies of five sands are given in this part of the report. These sands have been se-
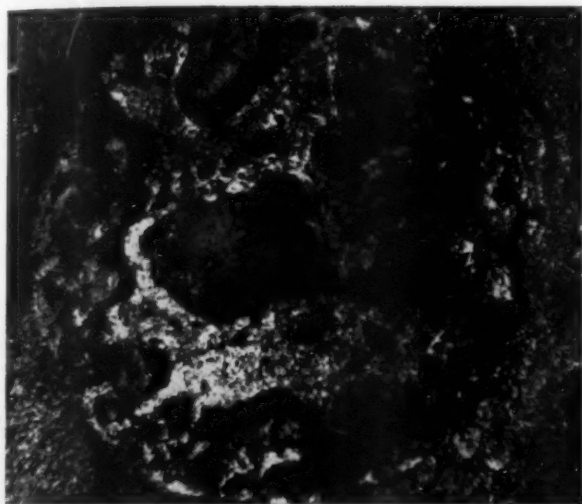
FIG. 27—SURFACE OF SAND K5 IN CONTACT WITH METAL. COMPLETE FUSION. PENETRATION .25 INCH. X1.5.

lected as representing five impurities present in the A.F.A. silica fraction. Details regarding these sands are given in Table 7, along with the chemical analysis of the A.F.A. silica fraction after sepa-
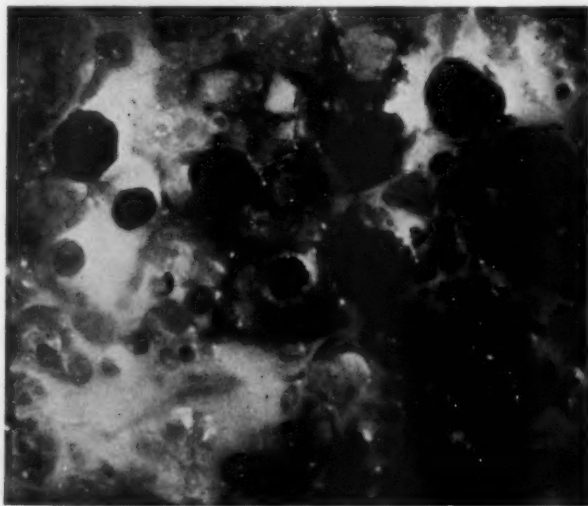

FIG. 28—SAME SAND SURFACE AS SHOWN IN FIG. 26 AT X25. MOLTEN SILICATE WITH UN-DISSOLVED SILICA GRAINS. OBLIQUE ILLUMINATION.

rating it from the A.F.A. clay fraction by a procedure similar to that recommended by the A.F.A., the only difference being that no sodium hydroxide was used as a flocculating agent. As sodium and potassium oxides were determined chemically, any addition of these elements would have interfered with the analysis. If enough A.F.A. clay was present to obtain sufficient material for analysis, it was analyzed separately.

57. Two of these sands, J6 and K5, contained little A.F.A. clay and would be classified, by this test, as suitable for steel
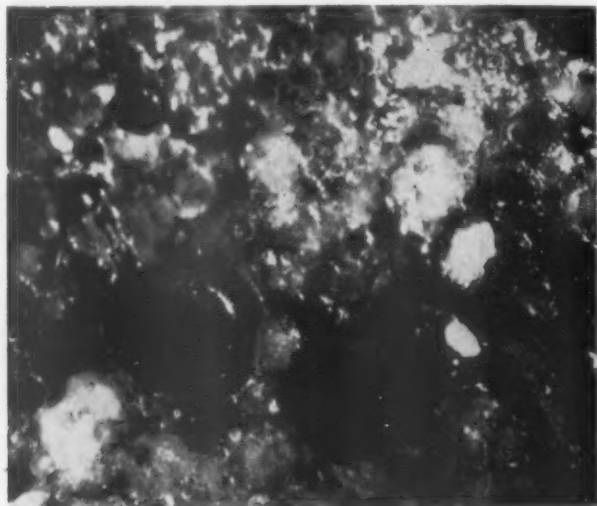

FIG. 29—SAME SAND SURFACE AS SHOWN IN FIG. 27 AT X25.   MOLTEN SILICATE WITH UNDISSOLVED SILICA GRAINS.

foundry use. One had a high lime content in the A.F.A. silica fraction, the other a high feldspar content. The other three sands, J7, J8 and J9, were high A.F.A. clay sands, two with an impure A.F.A. silica fraction and one with a pure A.F.A. silica fraction.

58. As can be seen from Table 7, the sintering points of the low A.F.A. clay sands were very much lower than those shown in Tables 5 and 6 for the synthetic sands, except those with large additions of strong fluxes. The behavior of the sands with impure silica when in contact with molten steel was very much different than the behavior of the synthetic sands with pure silica, even when large amounts of the same impurities were added to the A.F.A. clay phase.

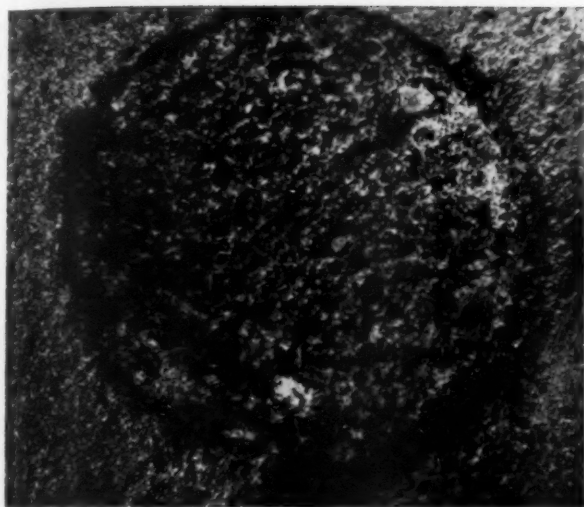59. *Effects of CaO + MgO and Na₂O + K₂O in Silica.* As

Fig. 30—High A.F.A. Clay Sand with Impure Silica, J7. Almost Complete Fusion. Penetration .12 Inch. X1.5.

shown by Figs. 26 and 27, the high A.F.A. silica sands containing 21 per cent $CaO + MgO$ and 11 per cent $Na_2O + K_2O$ in the A.F.A. silica fraction, melted completely when subjected to temperatures of molten steel. The penetration was so bad that it could
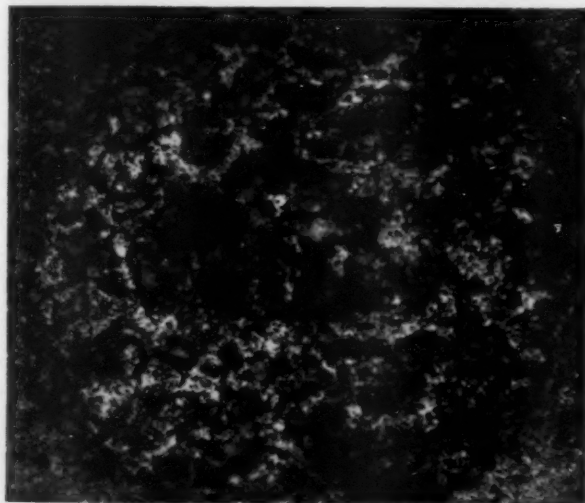

Fig. 31—High A.F.A. Clay Sand with Impure Silica, J8. Complete Fusion. Penetration .25 Inch. X1.5.

not be measured in thousandths of an inch as before, but had to be measured in tenths of an inch. Magnifications of 25X, as used in the previous photomicrographs, were much too high and a low magnification of 1.5X was used. The sand surface in contact with the molten metal was a molten glass containing undissolved silica grains, as shown in Figs. 28 and 29.
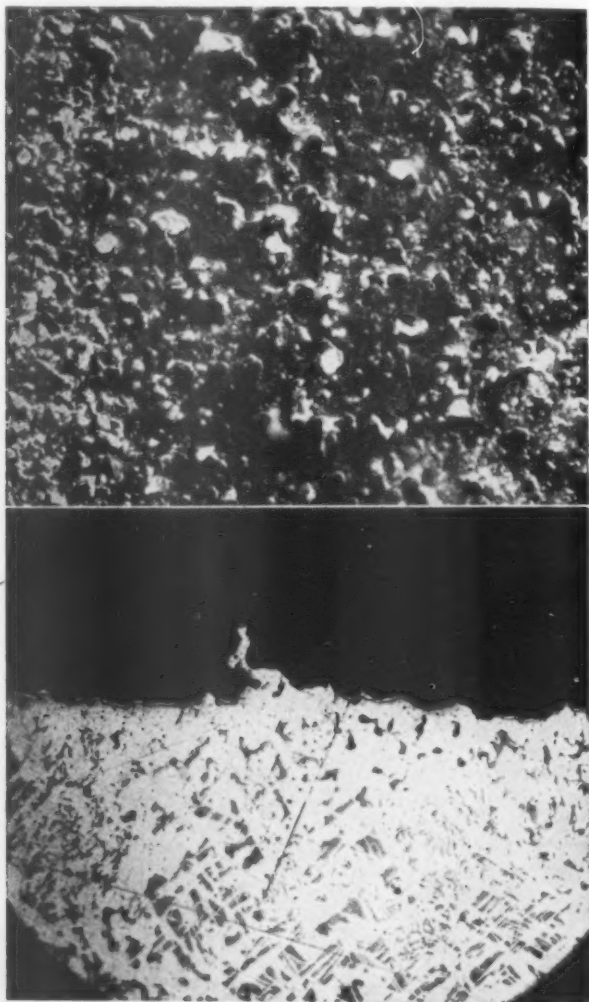

FIG. 32a—SAND SURFACE IN CONTACT WITH METAL. HIGH A.F.A. CLAY SAND WITH PURE A.F.A. SILICA, J9. X25. OBLIQUE ILLUMINATION.

FIG. 32b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 32a. PENETRATION .016 INCH. SURFACE SMOOTH. X25. VERTICAL ILLUMINATION.

Table 8

EFFECT OF IMPURITIES IN THE A.F.A. SILICA FRACTION ON THE SINTERING POINTS AND PENETRATION

Test No.	Per Cent Silica Sand B	Per Cent Impure Sand J6 or K6	Per Cent K_2O+Na_2O in Mix	Per Cent $CaO+MgO$ in Mix	Green Permeability	Sintering Point Degrees F. "A"	"B"	Penetration-inches
J1	100.0	0.0	—	0.0	170	2300	2825	.014
J2	95.0	5.0	—	1.0	150	2250	2725	.016
J3	90.0	10.0	—	2.1	170	2200	2725	.024
J4	75.0	25.0	—	5.2	170	<2200	2625	.040
J5	50.0	50.0	—	10.5	160	<2200	2450	.060
J6	0.0	100.0	—	21.0	135	<2200	2300	.50
K1	100.0	0.0	0.0	—	170	2300	2825	.010
K2	95.0	5.0	0.5	—	145	2250	2700	.023
K3	90.0	10.0	1.1	—	140	2200	2700	.038
K4	75.0	25.0	2.7	—	145	<2200	2600	.064
K5	50.0	50.0	5.5	—	160	<2200	2425	.15
K6	0.0	100.0	10.8	—	210	2000	2275	.50

60. *Study of Natural Bonded Sands With Impure Silica*. Sands J7, J8 and J9 were natural bonded sands as used in iron and non-ferrous foundries. The behavior of sands J7 and J8, containing impurities in the A.F.A. silica fraction, was entirely different than that of sand J9 with a pure A.F.A. silica fraction, when in contact with molten steel at 3000°F. Sands J7 and J8, with a high A.F.A. clay content and an impure silica fraction, broke down as can be seen from Figs. 30 and 31, but not as badly as the sands with a lower A.F.A. clay content and an impurer silica fraction.

61. *Study of Natural Bonded Sands With Pure Silica*. Sand J9, with a high A.F.A. clay content and a pure A.F.A. silica fraction, is interesting as an example of the other extreme. Its "B"
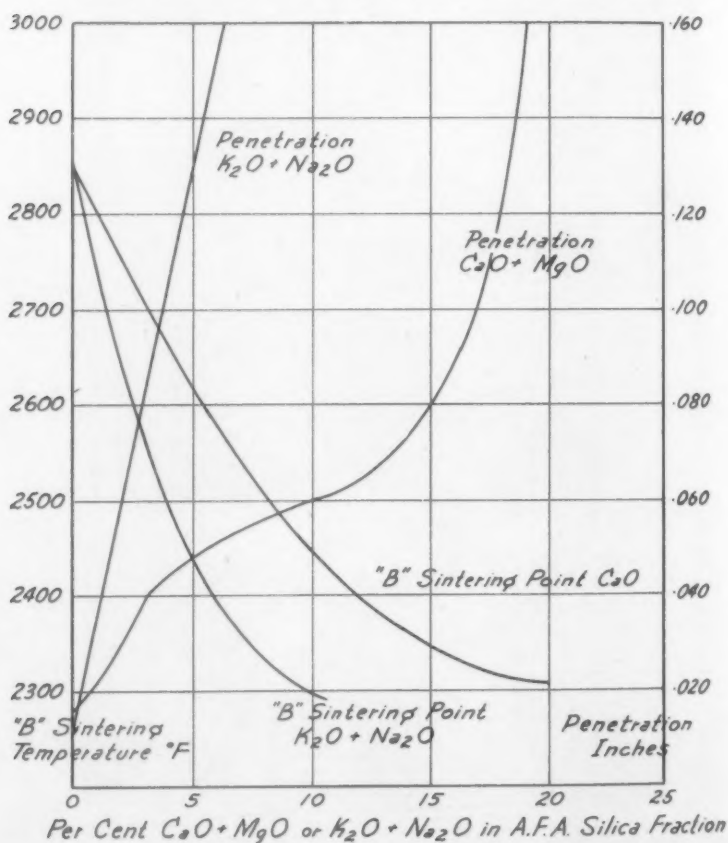

FIG. 33—Effect of CaO + MgO, Na$_2$O + K$_2$O in A.F.A. Silica Fraction on "B" Sintering Point and Penetration.

sintering point was much higher than that shown by the sands with an impure A.F.A. silica fraction, even though the grain size was much finer. The "B" sintering point of this sand was about the same as if the same colloid content had been added to a high silica sand to make a synthetic sand. Figure 32 shows that this sand
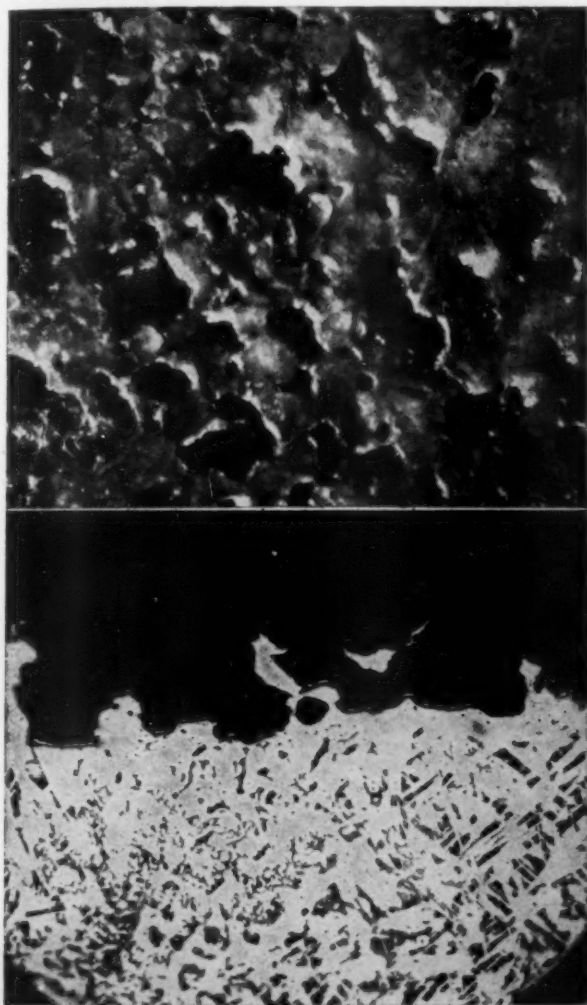

FIG. 34a—SAND SURFACE IN CONTACT WITH STEEL. 2.1 PER CENT CaO + MgO IN A.F.A. SILICA FRACTION. X25. OBLIQUE ILLUMINATION.
FIG. 34b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 34a. PENETRATION .024 INCH. SURFACE ROUGH WITH ADHERING SAND. X25. VERTICAL ILLUMINATION.

stood up very well when in contact with molten steel; in fact, it showed less penetration and adhering sand than the coarser synthetic sand usually used in the steel foundry. Although other properties of this sand make its use in the steel foundry impractical, as far as "burnt-on" sand is concerned, it could be used for steel.
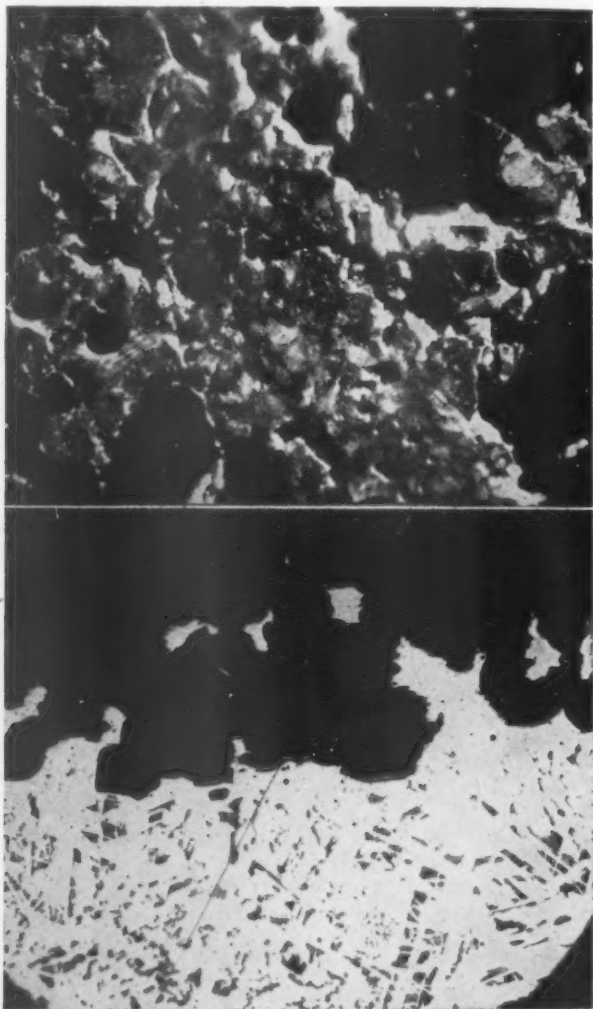

FIG. 35a—SAND SURFACE IN CONTACT WITH STEEL.   0.5 PER CENT $Na_2O - K_2O$ IN A.F.A.
SILICA FRACTION.   X25.   OBLIQUE ILLUMINATION.
FIG. 35b—SECTION THROUGH SAND-METAL INTERFACE SHOWN IN FIG. 35a.   PENETRATION
.023 INCH.   SURFACE ROUGH WITH ADHERING SAND.   X25.   VERTICAL ILLUMINATION.

62. It is interesting to note that if this sand had been analyzed chemically before separation into the A.F.A. silica and clay fractions, it would have been classified as an impure sand and put in the same class as sands J7 and J8.

63. *Determination of Amounts of $CaO + MgO$ and $Na_2O + K_2O$ Which Will Cause Penetration.* The high lime and feldspar sands, J6 and K6, were then mixed with the pure silica sand B to check the amount of $CaO + MgO$ and $Na_2O + K_2O$ in the A.F.A. silica fraction that would cause penetration and adhering sand in the steel foundry. Details of these mixes are given in Table 8. The "B" sintering points and penetration are plotted in Fig. 33. Figures 34 and 35 show the amount of fusion and penetration at the point when these sands started to break down. As can be seen from Figs. 33, 34, and 35, as little as 10 per cent of the high lime sand, J6, and 5 per cent of the high feldspar sand, K6, increased penetration and adhering sand appreciably. As sand K6 contained 10.8 per cent $Na_2O + K_2O$ and sand J6 contained 21 per cent $CaO + MgO$, 0.5 per cent $Na_2O + K_2O$ and 2.0 per cent $CaO + MgO$, *when present in the A.F.A. silica fraction,* are sufficient to cause trouble in a steel sand. If these elements are present in the A.F.A. clay fraction, larger amounts can be present without causing trouble as was shown in Table 6.

64. Increasing amounts of CaO, MgO, Na_2O and K_2O in the A.F.A. silica phase above those shown in Figs. 34 and 35 increased the amount of fusion, penetration and adhering sand as shown graphically in Fig. 33, until complete fusion occurred as is shown in Figs. 26 to 29.

65. Larger amounts of CaO, MgO, Na_2O and K_2O, in both the A.F.A. silica and clay fractions, may be present in iron and non-ferrous sands than in steel sands because of the lower temperature at the sand-metal interface. However, they must be controlled at some higher percentage, or serious trouble will be encountered.

SUMMARY

66. A study has been made of the mechanism of "burnt-on" or adhering sand, establishing the fact that, in steel practice at least, this adhering sand is due to penetration of the metal into the sand, mechanically locking it onto the casting. Adherence due to fusion plays only a minor part, if it enters into the picture at all.

67. Pure mechanical penetration due to too large voids being

present in the sand as rammed is the major cause of adhering sand
in the steel foundry. Decreasing the void size by decreasing the
grain size of the sand decreases penetration and adhering sand
in this case, although it decreases the refractoriness of the sand.

68. Although penetration of the sand by the metal and sub-
sequent mechanical locking of the sand to the casting is the cause
of adhering sand, this action can be caused by fusion and the open-
ing up of large voids and subsequent penetration of the metal into
these voids. The action after fusion and opening up of the voids
is the same as if voids of the same size had been present in the
sand as rammed and no fusion had occurred.

69. As adhering sand is caused by penetration, the tempera-
ture of the metal and sand-metal interface assumes greater impor-
tance than before. Not only must the sand be heated to a tempera-
ture high enough to melt any fusible constituent in order to cause
penetration, but it must be heated to above the melting point of
the metal. If for any reason, whether it be because the metal itself
is close to its solidification temperature, or heat flow through the
sand is so great as to keep the sand below this temperature, there
will be no penetration regardless of how fusible the sand or how
large the voids.

70. The action of silica flour, whether added to the sand mix
or added as a wash, is complex. A mixture of the fine grains of
silica flour and the coarser grains of the base sand, increases
penetration and adhering sand, but a uniform face of the fine
grains of silica flour in contact with the molten metal decreases
penetration and adhering sand up to a certain point depending
on the temperature of the sand-metal interface. Beyond this point
the silica flour breaks down suddenly and bad "burn-on" occurs.

71. Impurities present in the A.F.A. clay fraction do not lower
the "B" sintering point enough to cause adhering sand until
percentages much above those usually encountered in molding
sands are reached.

72. Much smaller percentages of the same impurities when
present in the A.F.A. silica fraction will cause trouble. This is
due to the fact that equilibrium is established much faster when
the impurities are present as silicates in the A.F.A. silica phase
than when they are present as a separate phase in the A.F.A. clay
fraction.

73. Strong fluxes such as borax, when added to the A.F.A.
clay phase to form a glaze which acts as sort of an automatic wash,

are too sensitive to temperature to be of practical value. For example, the correct amount of borax to give this glaze on the outside surfaces of a casting will cause bad penetration and adhering sand at reentrant angles and pockets of the same casting. The steel foundryman is using a glaze when he adds silica flour to the sand under proper conditions. As has been shown, this silica flour glazes and forms a very viscous film preventing metal penetration. The fact that bentonite and crude sands tend to give better peel is due to this same condition. Both lower the sintering point slightly, and cause a glazing action. The use of iron oxide with pure silica sands, in addition to having other advantages, also lowers the sintering point slightly and again gives a viscous high silica glaze.

74. The most fool proof method whereby the steel foundryman can decrease adhering sand in the foundry is to use as fine and as uniform a sand as will give him the other physical properties required. The "B" sintering point should be controlled at about 100°F. lower than the average pouring temperature of the metal.

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3. *"Third Report of Steel Castings Research Committee,"* BRITISH IRON AND STEEL INSTITUTE, p. 130 (1938).

4. *"Progress Report of the Sintering Test Subcommittee, Foundry Sand Research Committee,"* TRANSACTIONS, AMERICAN FOUNDRYMEN'S ASSOCIATION, vol. 49, pp. 552-558 (1941).

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DISCUSSION

Presiding: DR. H. RIES, Cornell University, Ithaca, N. Y.

EARL E. WOODLIFF[1]: Mr. Caine has certainly done a fine job in presenting this paper, but I cannot agree 100 per cent in the analogy of his penetration.

First of all, I like to tie the laboratory results with actual results obtained in the foundry a little closer. In my work on sands, from the non-ferrous up through the steel range of metals, I find that today probably the most important defect occurring generally throughout the foundry industry is this penetration. I cannot entirely attribute this condition to a lack of sintering or a lack of refractoriness.

I recently had the experience of working on bronze piston rings which were ¼-in. in cross-sectional area. These bronze piston rings really had penetration. They were poured at about 2000°F., and I believe that most of the sands that we are using today will stand at least 2000°F. without sintering, and they had penetration of 1/16-in. On the other hand, I have made heavy steel castings which show no penetration, even though we know that the sintering point of the sand is considerably below the pouring temperature.

I think that these voids or this cracking of the sand is a result of the thermal shock to the sand and, to a large degree, is the cause of the penetration. In other words, Mr. Caine has noticed that the fusion of the sand opens voids. I should think it more logical to believe that these voids occur before the sand fuses.

MR. CAINE: The case of penetration with bronze which Mr. Woodliff mentions is typical of most all copper base alloys. Not only do these alloys have a very low surface tension, but the sands used almost invariably have sintering points too high for the pouring temperatures involved. These cases are almost always ones of mechanical penetration, not penetration due to fusion. In two cases, of which I know, lowering the sintering point of the sand used for this type of casting has helped appreciably. When we consider that most penetration with steel castings poured at 2900° to 3200°F. is due to mechanical penetration and fusion, it is not surprising that fusion is insignificant in the problem of "burnt-on" sand with metals of lower melting points. This mechanical penetration should not be confused with veining or cracking.

MR. WOODLIFF: The definition of penetration is very vague. We call everything penetration. Even pimples on the casting, or roughness of the casting. We should, first of all, define what penetration is before we discuss it too seriously.

The penetration I am speaking of is the penetration back of the wash, if there is wash applied, which is usually done on heavy work, and the wash apparently never has been disturbed, from its physical appearance to the naked eye.

Nevertheless, you will have a combination of sand and metal which is magnetic. You can pick up these penetrated sand particles with a magnet. Therefore, I think you have penetration which is mechanical,

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[1] Foundry Sand Service Engineering Co., Detroit, Mich.

and I personally think, based upon my observation in doing some work with temperature shock, that the penetration is due to the cracking or the opening up of the voids of the sand at low temperatures.

Possibly, as pointed out by Mr. Albus and Mr. Pragoff, these cracks appear by just lowering the furnace over the sample for the matter of a few seconds—I am speaking of the dilatometer now—before the sample of sand has an opportunity to reach a very high temperature. The temperature may be as low as 500 or 1000°F. These cracks already may have formed in the material. If they are going to form, they will probably do so at a low temperature.

In other words, I believe it is low-temperature thermal shock that opens the first surface of the sand and allows penetration.

I have observed, too, that the grain distribution has a lot to do with it. One of our jobs was a heavy iron casting. We were able to cure penetration by the addition of 25 per cent silica flour. Additions of less than 25 per cent did not have much effect in decreasing the penetration. That, of course, lowered the permeability considerably. At the same time we went to the other extreme and added gravel to our sand. I am speaking of gravel as 20-mesh grain material. An addition of about 10 per cent gravel stopped the penetration.

I feel that, in foundry sands, we are working too much toward a narrow grain distribution. I think that when this is more generally realized we may be on the road to stopping penetration.

CHARLES P. ALBUS[2]: In regard to veining of core and molding sands about which Mr. Woodliff was speaking. We have noticed that sand samples which cracked badly developed this condition, on shock heating, within the first 1½ min. after specimens were placed in the dilatometer. Those who have dilatometers can check this. Place your specimen in the dilatometer and let it soak for one or two min. You will find, on lifting the dilatometer furnace, that the specimen has cracked within that time. Maximum expansion of the sand also occurs within the same interval, indicating that a close relationship exists between the two.

MR. CAINE: Mr. Woodliff seems to be confusing penetration, as discussed in this paper, with veining and other defects due to dimensional instability. This point is emphasized in paragraph 23 of the paper. These two phenomena are entirely different and can be distinguished. When we have penetration we have uniform penetration of the metal into the sand, even though the penetrated areas are localized on the casting, if, for example, penetration is due to soft ramming or low flowability. When we have veining due to dimensional instability, we have definite fissures with metal penetration into these fissures. The sand in between these fissures shows no penetration. Although these two conditions may sometimes be confusing on visual examination, they cannot be mistaken when examined under the microscope.

J. A. RASSENFOSS[3]: About this penetration, Mr. Caine's idea is that penetration will not occur unless the sand temperature is above the

fusion point of the metal itself. Mr. Woodliff brought out that there are quite a few cases where we sometimes get simple mechanical penetration which is evidently not due to fusion. Sometimes we have noted that the penetration can be so great that it would not seem possible for the sand to be heated to a temperature, or above the temperature, of melting for this particular steel.

I have in mind a rather small casting. It was small in weight, possibly 60 lb., and we purposely made a casting, simply a steel slug, to study penetration. On the inside was a cone of sand about 2-in. in diameter at the top and possibly 3-in. in diameter at the bottom, the total height of the cone being 3 in.

We made this mold with a simple bentonite-sand-water mixture. There was about 5 per cent bentonite and the sand was a washed and dried silica sand, A.F.A. fineness No. 40. It had a very narrow distribution, and it was concentrated on about three sieves, thus having a very high permeability. We purposely poured steed into that mold, rapidly and very hot, about 3000°F. That cone of sand was completely penetrated with metal all the way through. The sand grains were preserved intact.

Of course, you can see that it is hardly probable that, in a casting of this size, the metal adjacent to the sand face would remain molten for a sufficiently long time for the center of the sand core to get up to the actual melting point of the metal.

Therefore, I think we should be careful, for we may have penetration even with a sand that has not been heated to as high a point as the melting temperature of the metal. If the voids are unusually large, the metal will probably penetrate farther into the sand.

MR. CAINE: The exact answer to Mr. Rassenfoss' remarks will have to await data of heat conductivities of molding sands, a very important problem that has been absolutely neglected.

Such data as are now available do show that the core in Mr. Rassenfoss' casting will be heated throughout to above the melting point of steel before the metal starts to freeze. Although Mr. Rassenfoss does not give the overall dimensions of his casting, he mentions a "slug" weighing about 60 lb. This would correspond to a casting 6½-in. in diameter, 6½-in. high with a volume of 0.125 cu. ft. The volume of the core is 0.0087 cu. ft., 7.0 per cent of the volume of metal. As we know the specific heat of steel and silica, we can calculate the amount of heat absorbed by the core and the amount of heat lost by the metal in heating the core to the temperature of the metal from the following equation:

$$(1) \qquad\qquad H = c\, m\, (T_2 - T_1)$$

Where H = heat evolved or absorbed, B.t.u.; c = specific heat; m = mass in lb.; T_2, T_1 temperature °F.

Substituting in equation (1) the pouring temperature and freezing temperature of the metal,

$$H = 0.182 \times 60 \times (3025 - 2700)$$

is equal to 3550 B.t.u. evolved by the steel while cooling 325°F. from the pouring temperature to the beginning of solidification.

A similar substitution in equation (1) for the sand core,

$$H = 0.276 \times 0.89 \times (3025 - 60)$$

gives 713 B.t.u., the amount of heat required to heat the core throughout to above the melting point of steel. This is 20 per cent of the heat evolved by the metal during the 325°F. drop in temperature between pouring and solidification. This is equivalent to chilling the metal only 65°F., so there is plenty of heat available to heat the core to above the melting point of steel. The only question now is, will heat penetrate to the center of the core before the metal solidifies.

Freezing times for sections of from 1 to 10 in. in steel molding sands have been determined by the writer. These values check very closely with those of Briggs and Gezelius[4]. It was found that it requires 12 min. for the center of a 6½-in. round to solidify in sand when poured at 2950°F. As the thermal conductivity of sand is only 3 per cent that of steel, heat loss through the one end of the core is negligible, so, for all practical purposes, we can consider the casting as solid, after deducting 65°F. used in heating the core. Therefore, we have at least 12 min. for the heat to penetrate the core, with metal following as soon as the sand has reached a temperature near the liquidus line.

The rate of heat transfer through a mass, when the temperature of the mass is changing, can be calculated from a partial differentiation of Fourier's law. Brown and Marco[5] and Williamson and Adams[6] have solved this equation and, by dimensional analysis, have worked out an equation for our problem, a cylinder subjected to heat from all sides, as follows:

$$(2) \qquad T_c = T_s - \left[(T_s - T_o) \, f_5 \left(\dfrac{\frac{k}{pc} \cdot t}{D^2} \right) \right]$$

Substituting in equation (2), T_s = temperature of the metal in contact with the surface of the core at any given time; T_o = temperature of the core before the casting is poured; k = 0.95 B.t.u./sq. ft./hr./°F./ft.; $c = 0.276$; p = 102 lb./cu. ft.; t = time in hours; D = diameter 0.208 ft., and solving for t, the curve shown in Fig. 36 of this discussion is obtained. The symbols f_1 and f_5 in equations 2 and 3 denote functions of the quantities in parentheses. The values of these functions depend

[4] Briggs, C. W., and Gezelius, R. A., "Studies on Solidification and Contraction in Steel Castings, III," TRANSACTIONS, American Foundrymen's Association, vol. 43, p. 274 (1935).

[5] Brown and Marco, INTRODUCTION TO HEAT TRANSFER, McGraw-Hill Book Co., New York, 1942.

[6] Williamson, E. D., and Adams, L. H., "Temperature Distribution in Solids during Heating or Cooling," PHYSICS REVIEW, vol. 14, pp. 99-144 (1919).

not only on how the mass is heated (from one side or from all sides)
but also on its geometry. Anyone interested in details and values for
f_1 and f_5 can find them in reference 6.

As can be seen from Fig. 36, the center of the core is heated to above
the liquidus line for low carbon steel in 10 min., within the time required
for a section of this size to freeze. The results are, if anything,
conservative, as turbulence and presence of liquid phase below the liq-
uidus line have been neglected. The thermal conductivity of molding
sand has been derived from values for Chamotte published by
Chworinoff[7], taking into consideration the relation between his freezing
times and those determined by Briggs and Gezelius and the writer with
sand. This value of 0.95 B.t.u./sq. ft./hr./°F./ft. is also conservative.
The effect of permeability and heat transfer by convection is, in this
case, negligible, as the core is covered by metal. If the core had extended
through the casting, convection would have to be taken into considera-
tion.

These results can be checked by using Fourier's equation to determine
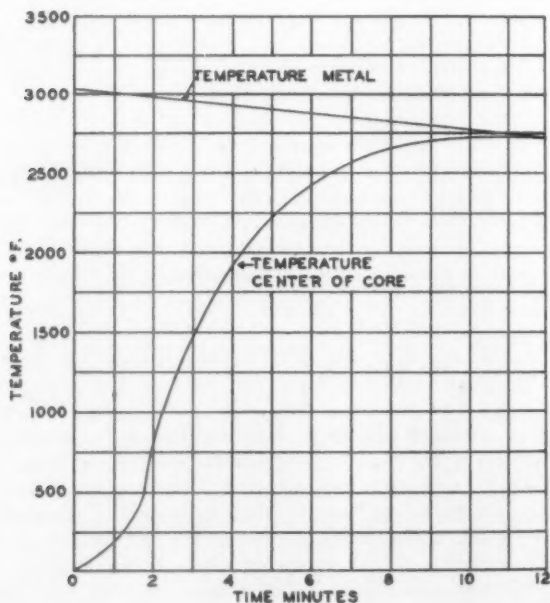the heat penetration into the outside surfaces in contact with the steel.

FIG. 36—CURVE SHOWING THE RISE IN TEMPERATURE AT VARIOUS TIME INTERVALS IN A
2½-IN. DIAMETER CORE SURROUNDED BY METAL POURED AT 3025°F.

[7] Chworinoff, N., "Theory of the Solidification of Castings," GIESSERIE, vol. 27, pp.
177-225, British Iron and Steel Institute Translation No. 117.

Solving this equation for an infinitely thick mass subjected to a change in temperature on one surface only, equation (3) is obtained.

$$(3) \qquad T_x = T_s - \left[(T_s - T_o) f_1 \left(\sqrt{\frac{x}{\frac{k}{pc} t}} \right) \right]$$

where x = distance from the sand metal interface.

As Briggs and Gezelius[5] have shown that the outside surfaces of a casting in contact with sand solidify in less than 5 sec., solving equation (3) with t = 5 sec., we find that only the first 0.015 in. of sand on the outer surfaces is heated to above the melting point before the metal freezes. If 5 secs. were requird to pour the casting, 0.028 in. of the sand in contact with the moving metal was heated to above the melting point. This means only a rough surface and some adhering sand on the outer surfaces while the core in the center was heated throughout and, consequently, penetrated by the molten steel.

JOHN HOWE HALL[8]: I want to call attention to one thing that Mr. Caine did not elaborate upon; that is, the sample where he put salt in his mixtures. As I understand the micrographs, when he added salt he got enormous voids and no penetration. He took refuge, of course, in the effect of the gas produced by the salt by the high temperature, assuming that this gas kept the steel from entering the voids. That explanation may or may not be correct.

I also want to call attention to the fact that, in studying sand penetration, it is necessary to distinguish between the effect when the steel rises quietly over the sand and when the steel flows rapidly over the sand near the gate. Mr. Caine's method tests the sand by means of steel rising quietly over the end of a core and, judging by this test, it is suggested that partial fusion of the sand is a rather good thing. In fact, Mr. Caine said that sometimes penetration is due to a too high fusion point of the sand. I think that is true where there is no bodily flow of the metal over the sand; where there is intense flow of the metal over the sand, I doubt if it is true.

A number of those present will recall that a few years ago an extensive research was conducted with the idea of preventing adhering sand near the gates of certain castings made on a large scale. The final remedy suggested was the use of sands which would fuse and give a glaze. These recommendations went out to the foundries concerned, and a facing was tried that fused and made a semi-liquid skin near the gates of thin castings of large area, where there had to be a great deal of heavy flow of metal over the sand. The result, as I can personally testify, was a great deal worse burning-on than if there had not been any special facing used. I believe that, as that semi-liquid face forms, if there is a rush of metal over the mold, the metal carries that liquid

[8] General Steel Castings Corp., Eddystone, Pa.

face away with it, but this does not occur when the metal is rising quietly over the sand.

Finally, on this matter of refractoriness, a recent discussion disclosed that, in cement sand molding for steel, adherence and penetration of sand near the gate is taken care of by the use of material of a very refractory nature. That practise I originated in my consulting work for the people making cement-sand molds. I had supposed it was entirely confidential but, as their representative discussed it openly yesterday, I venture to comment upon it briefly. Cement-bonded sand tends to penetrate and give adherence of sand near the gates. The originators of cement molding, in France, carried on intensive research to try to render cement-bonded molds for heavy steel castings free from this trouble. It was taken care of in this country by the expedient of using aluminum oxide fines, 95 per cent through 200-mesh, bonded with cement and having a permeability of about 4.

Up to a certain point the use of that mixture—100 lb. of aluminum oxide fines, 13 lb. of cement, 8 to 9 per cent water—used as a facing near the gates absolutely prevents sand penetration, although there is some slight fusion of the cement in contact with the steel. However, when that material is used for a small core with a heavy body of metal around it, it will penetrate deeply and, when aluminum oxide fines are penetrated by steel, we have quite a problem. It is hard to chip out a penetrated sand core, but it is considerably more difficult to chip out penetrated aluminum.

In both the case of the casting where the sand is not penetrated and in the case of the casting where the steel penetrates into the mold, we have incipient fusion of the cement and aluminum oxide. However, it is only in the small cores surrounded by a very large mass of metal that incipient fusion results in penetration and, when it occurs, it is the type of penetration shown by Mr. Caine. A core 2-in. in diameter will be 50 per cent sand and 50 per cent alumina to the center.

Having learned that a facing of that nature would prevent penetration in most cases, we hoped that a wash would do the same thing and be cheaper, so we made up washes of alumina and spread them on cement-bonded sand, but the more we washed the molds, the worse we made them. The penetration was a great deal worse in the cement-bonded silica sand when using alumina wash. That, of course, would tend to bear out Mr. Caine s theory about fusion leading to penetration.

Getting off cement-sand molding and back to ordinary sand, I have also seen some work upon the effect of wash, which indicated that, in a great many cases, the mechanism of penetration back of the wash is that the wash cracks, some being large cracks and some very fine cracks. The steel penetrates through the cracks and then spreads in the sand behind the wash.

MR. CAINE: Mr. Hall has put his finger on the one exception to the theories of this report; sands containing salt. These tests were included in the paper for two reasons. First, that this exception to the rule should find some practical use. The other is that I have always been

sold on the idea that exceptions are just as important as checks and should be reported.

Mr. Hall's experience with increased penetration when a large volume of metal flows over the sand surface is familiar to foundrymen. This point was not brought up in the paper for the same reason that the student of mechanics always studies Statics before he undertakes Dynamics. As can be seen from the answer to Mr. Rassenfoss' discussion, the solution of the rate of heat transfer to sand is quite complicated, even under static conditions, without adding the kinetics of moving metal.

Chworinoff[*] has recently published some data on this point. He has divided the increased heat input due to moving metal into two factors; one the additional heat absorbed by the sand from metal that moves to another part of the mold, the second a turbulence factor. He shows that when these two factors are superimposed, as would be true when there is a heavy flow of metal over the sand, the solidification time is increased as much as 33 per cent. As this is equivalent to increasing the pouring temperature by as much as 100°F., it is not surprising that we have penetration at these areas. As was shown in the paper, a 50°F. increase in pouring temperature was sufficient to double and triple the amount of penetration, even with sluggish high-silica sands. If strong fluxes are added in an effort to form a glaze, the same increase in solidification time can mean complete breakdown.

Mr. Hall's experiences with cement-bonded sands and with alumina check with sintering point determinations. The "B" sintering point of 13 per cent cement sand is 2550°F., 250 to 300°F. lower than the usual steel foundry facing sands. Alumina wash decreases it still further, unless the wash is very heavy. These sands are the only sands I have found in use, east of the Mississippi river, in steel foundry production where penetration and adhering sand conditions are due to fusion. We have had very good results with heavy coats of silica-flour wash on cement molds. A thin coat seems to be no better, or worse, than none at all. Although the silica-flour wash only raises the "B" sintering point of the surface to 2600°F., the sluggishness of relatively pure silica gives us the results we want.

Penetration of metal back of the wash on clay-bonded sands, mentioned by Mr. Hall, is illustrated in the paper by Fig. 20. I have seen this type of penetration due to cracking of the wash, and also because of incipient failure of the wash due to fusion. In border line cases that occur occasionally, when the wash just starts to fail, the penetrated area is still separated from the casting and can be peeled off the casting as one would peel a banana. There is so much steel in this penetrated area that it is actually flexible.

[*] Ibid.

Design of a Front Slagging Cupola Spout

By R. D. Petcher*, Michigan City, Ind.

1. At the request of the A.F.A. Cupola Research Committee, we are glad to present the details of a front slagging spout which has been used very successfully at our plant for some time. Like most front slagging spouts, both the molten metal and slag are carried together through the cupola breast or tap hole into a trough. The metal then flows under a slag dam, while the slag floats on the metal and runs off at the side of the spout into a slag ladle. The clean metal then flows over the lip of the spout into a receiver from which it is distributed, by means of conventional transfer ladles, to the molding floors.

2. The dimensions in Fig. 1 are those of our own spout which is used on an 84-in. cupola shell lined to 66-in. It operates at approximately 18-oz. wind box pressure. This construction can be applied to any operation by proportioning it to suit a particular size cupola. The distance between the top of the cupola breast or tap hole and the lip of the slag spout is critical. If it is too low for the wind box pressure, air will bubble out of the tap hole, and if it is too great, the slag will not flow from the well of the cupola. The table of distance "X" and wind box pressure is taken from a paper by Reese[1].

MELTING PRACTICE

3. It is our practice to start melting with the cupola tap hole plugged. The slag spout drain hole is plugged with a round stock core. About 18 min. after "wind on," the cupola tap hole is opened and the metal flows continuously until the end of the heat.

* Foundry Superintendent, Manufacturing Div., Weil-McLain Co.
1 Reese, D. J., "Cupola Operation," TRANSACTIONS, American Foundrymen's Association, vol. 46 (1938), pp. 173-194.
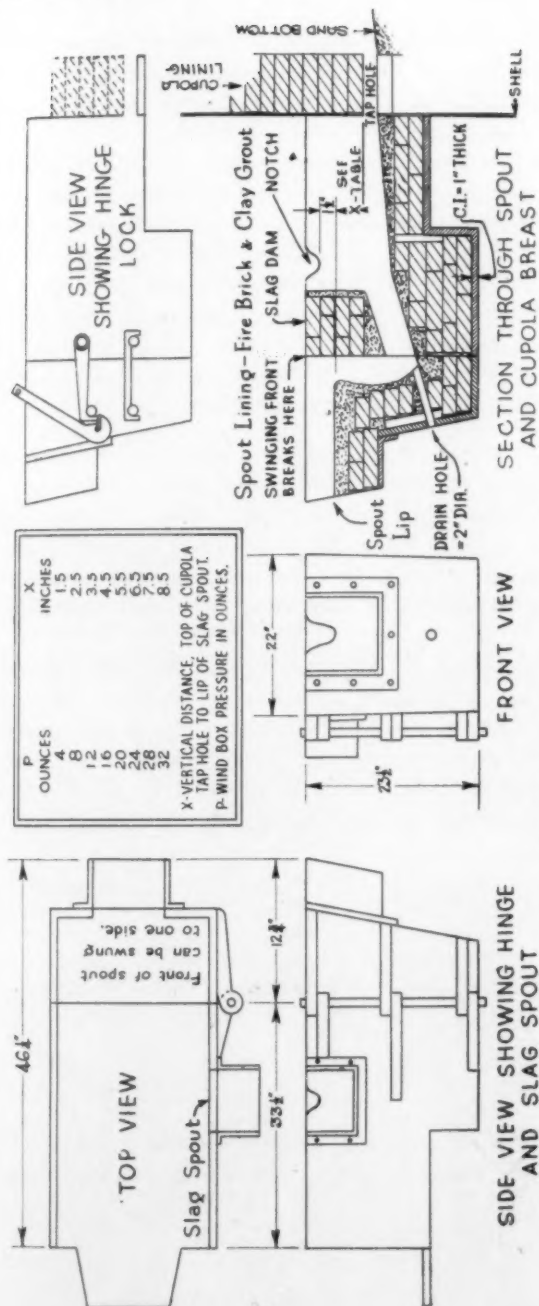
Fig. 1.—Section Drawing of Front Slagging Cupola Spout.

Melting can be stopped by simply shutting off the wind, and the
slag spout will remain open for as long as 45 min. To shut down
for longer periods, the slag spout drain hole should be opened,
allowing the spout and cupola well to drain.

4.   As the last iron in the stack melts, about 2000 lb. of usable
iron will remain in the cupola well and slag spout. This is tapped
out by opening the cupola drain spout. When slag comes, the drain
spout is lightly plugged. In due course, the cupola bottom is
dropped, after which the front of the drain spout is unlatched
and swung to one side. The whole spout system then drains free
of molten iron and slag.

MAINTENANCE COST

5.   The cost of daily maintenance in labor and materials is about
the same as that of a standard cupola spout. The slag is easily
chipped out, holes are filled with the same grout mixture used
to repair the cupola lining, and the spout is then washed with
daubing clay and dried with either a torch or a wood fire. The
whole spout, including the slag dam, is relined once in about 6
months.

6.   In our opinion, the low maintenance is due to massive, rigid
construction and large holes and passages that are easily cleaned.
The spout is made of cast iron sections of $\frac{3}{4}$- to one-in. thickness,
is very rigidly supported by the cupola frame at one end and is
rigidly suspended from a steel building truss at the other end.
Heavy lateral tie rods at the front end prevent side sway.

Stress Relief and the Steel Casting*

Effect of Temperature on Rate of Release of Stress in Plain Carbon Steel

By E. A. Rominski**, and H. F. Taylor**, Washington, D. C.

Abstract

The problem of determining proper cycles for stress relieving steel castings, weldments, and other fabricated parts is complex and in the past has defied accurate analysis by any simple means. Locked up internal stresses resulting from the method of manufacture and/or heat treatment may affect the service life and dimensional stability of structures, and parts are generally given a final stress relieving treatment at temperatures between 550 and 750°C. (1020 to 1380°F.) to reduce them to a minimum. Removing stresses at the temperature of the furnace does not necessarily insure a stress free part as conditions of cooling may reestablish stresses of a high order. Unless proper precautions are taken, even dead annealed castings may contain internal stress. The problem undertaken at the Naval Research Laboratory is in two parts, (1) a determination of rates of relief of stress as a function of temperature, and (2) a stress analysis of castings under various conditions of cooling. It is believed that a method has been found to solve the first part of the problem in the manner discussed in this paper. The second part is well under way and will be presented upon completion of the work. This paper describes a special method for determining relaxation rates of steel or other metals. Using a few simple measurements upon any type of steel, it is possible to calculate the time required at any temperature to reduce the stress from any initial value to any final value. In other words, the stress relieving time under prescribed conditions can be readily computed with sufficient accuracy from two or more observations upon each kind of steel at each of two or more temperatures. Such data have been worked out for plain carbon steel at temperatures between 550° and 750°C. (about 1020 and 1380°F.) and are presented herein.

Introduction

1. The usual method of cooling a casting or a structure from the high temperatures employed in commercial heat treating or
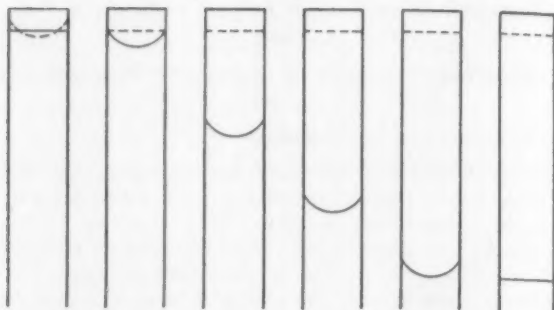

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FIG. 1—DIAGRAM ILLUSTRATING IN SEVERAL STEPS HOW THE COOLING OF A STEEL ROD AT
A UNIFORM RATE FROM A HIGH TEMPERATURE ACQUIRES INTERNAL STRESS WHEN THE
COOLING IS STOPPED.

fabricating practice leaves the steel in an unstable condition due
to stresses arising from non-uniform contraction. These stresses
may be as high as the yield strength of the material and, therefore,
in alloy steels will be higher because of the higher yield strength.
Such steel is said to be ''stressed'' and, if the stresses are excessive,
they are always a potential source of danger in service and often
will interfere with the dimensional stability in machining. If the
stressed piece is heated to a sufficiently high temperature and then
slowly cooled, it may lose a considerable part of its internal stress.
This operation of removing or diminishing the stresses is called
''stress relieving'' or ''relaxation.''

2.   The removal or prevention of excessive stress is one of the
many difficult problems encountered by steel foundries. When a
casting is poorly stress relieved, large losses by cracking or warp-
ing may occur when being welded, machined or even after being
placed in service. A proper and efficient method of formulating
the stress relief cycle, through an understanding of the phenomena
of plastic flow of metals under stress, is therefore necessary to the
manufacture of stress-free steel castings.

3.   The stresses developed within a casting from temperature
gradients established during heating or cooling can be simply and
usefully analyzed in the manner employed by Adams and Wil-
liamson in their classic presentation on the annealing of glass. As a
matter of fact, at temperatures relatively high for the two mate-
rials, metal resembles glass in many respects, except that the me-
chanics of strain measurement are considerably more complicated.

Effect of Temperature on Stress
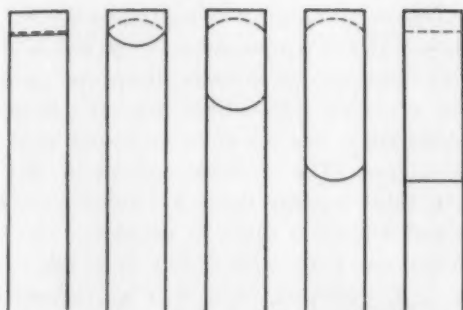4.   Steel castings are usually of complicated design and made

Fig. 2—Diagram Similar to Fig. 1, Except that the Rod Is Cooled from a Low Temperature, in Which Case No Permanent Stress Is Introduced.

up of sections of various thicknesses. However, in considering the effect of temperature on stress and actually in considering stress relief in general, the problem may be greatly simplified by discussing cylindrical elements, the concentric layers of which undergo stress and temperature differentials similar to the many parts of the casting.

5. The behavior of stresses in plates of glass has been analyzed by Adams and Williamson[1], and their method is useful for steel. Let us first consider the case of a stress-free bar of steel at room temperature heated uniformly at a rate and within a range such that the proportional limit is not exceeded, *i.e.*, it behaves like an elastic body. The outer layers, being hotter, expand from the heat more than do the inner and are placed in longitudinal compression, while the inner layers will be in tension. In changing from maximum compression to maximum tension the intermediate layers
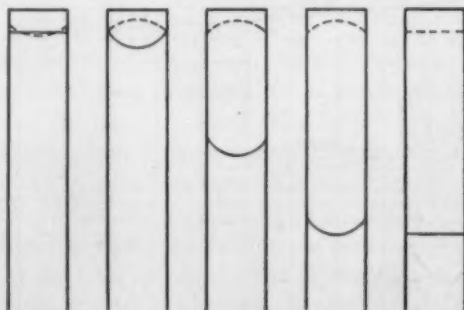

Fig. 3—Diagram Similar to Fig. 1, Except that the Rod Is Cooled from an Intermediate Temperature. In This Case, Stress Is Introduced but Less Than that Amount When Cooled from a High Temperature.

[1] Superior numbers refer to references at end of paper.

will be under stresses of varying magnitude with an intermediate
zone of zero stress. If the temperature of the rod is allowed to be-
come uniform throughout, the stresses disappear upon removal of
the temperature gradient. In cooling, the bar will go through an
equal but opposite stress cycle, *i.e.*, compression at the center and
tension at the surface. The stresses induced in this manner are
temporary since they depend upon a temperature gradient for
their existence and disappear upon its removal.

6. When steels are heated or cooled at a rate such that the
proportional limit is exceeded, or if they are heated high enough
for plastic flow to occur readily, permanent stresses may be in-
duced upon removal of the temperature gradient causing them.
An example of this is cooling a bar of steel at a steady rate from
a uniformly high temperature at which stresses are readily relieved.
Before cooling starts the bar is stress free and even while the tem-
perature gradient is being established the resultant stresses are
being removed by plastic flow since the steel is at a temperature
sufficiently high to permit it. The temperature gradient reaches
a steady, parabolic state rather quickly as shown by Williamson
and Adams[2], and the parabolic form persists as long as the rate of
removal of heat is constant. The condition is one in which there
is a parabolic temperature gradient but no stress gradient, and in
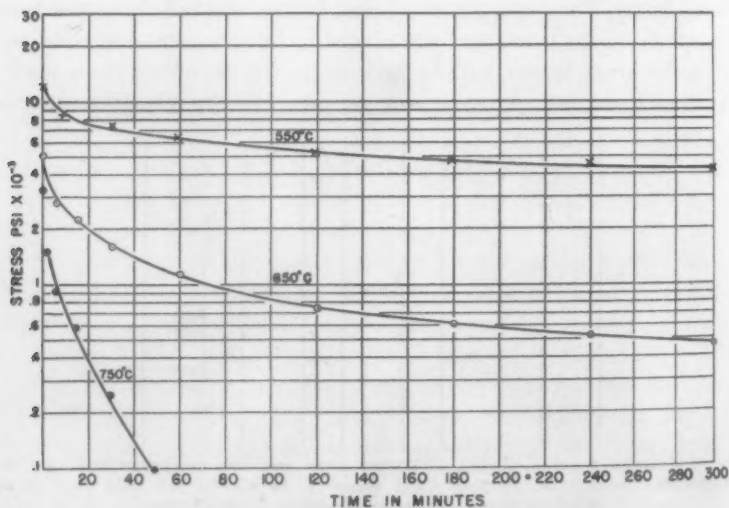this condition the bar will be stressed when the temperature gra-

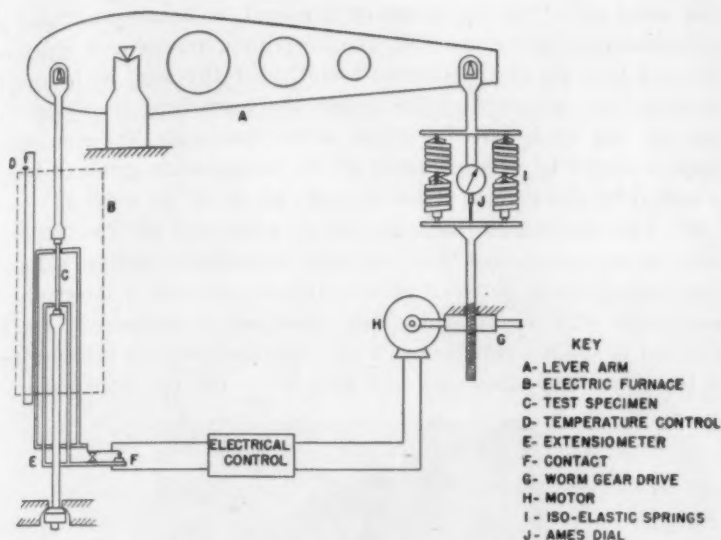FIG. 4—RATE OF CHANGE OF STRESS WITH TIME IN PLAIN CARBON STEEL.

FIG. 5—SKETCH OF AUTOMATIC RELAXATION MACHINE.

KEY
A- LEVER ARM
B- ELECTRIC FURNACE
C- TEST SPECIMEN
D- TEMPERATURE CONTROL
E- EXTENSIOMETER
F- CONTACT
G- WORM GEAR DRIVE
H- MOTOR
I - ISO-ELASTIC SPRINGS
J- AMES DIAL

dient is finally removed at room temperature or at any other relatively low temperature. Thus, the rate of cooling is of secondary importance to the temperature at which cooling starts and it is very possible to have more stresses in castings which have been annealed than in others cooled more drastically. The simple graphical method devised by Adams and Williamson[1] to illustrate the interdependence of temperature and stress in glass serves to explain the above conditions more clearly than words.

Graphical Representation of Stresses

7. Figure 1 represents the condition of cooling just described in which the bar of steel was originally stress-free and at a relatively high uniform temperature. The solid lines represent temperature and the broken lines stress distribution across the bar. As discussed above, the stresses set up during establishment of the temperature gradient are relieved and as long as the cooling rate is uniform the bar remains free of stress. It is at the final temperature of cooling, when the bar is reaching a uniform state and the parabolic cooling gradient is disappearing that the stresses reappear.

8. Figure 2 illustrates the condition of cooling from a temperature below that at which appreciable stress relief occurs. In

this case while the temperature gradient is being established a corresponding stress gradient is developing, no part of which is relieved because of the limited mobility of the steel at the lower temperature. Removal of the temperature gradient serves only to put the bar back into its initial stress-free state, the temporary stress induced by establishment of the temperature gradient being balanced by the reverse stress brought about by its removal.

9. The conditions discussed above illustrate the two limiting cases in the development of internal stresses in cooling castings. For cooling cycles between these extremes, stresses of intermediate magnitude will be induced. This condition is pictured in Fig. 3. The rod is again stress-free at a uniform temperature between that of the above two cases and such that while the parabolic tempera-
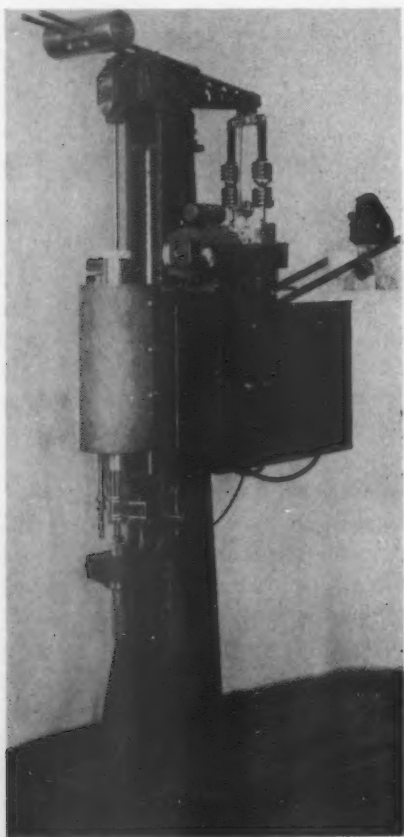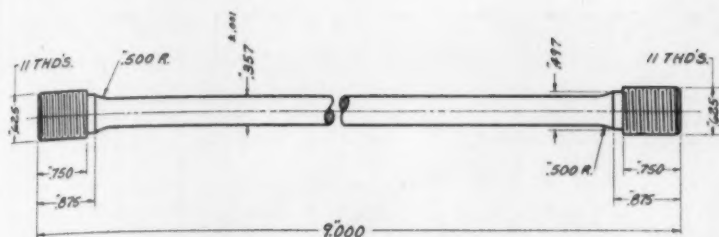

FIG. 6—RELAXATION MACHINE.

FIG. 7—RELAXATION TEST PIECE.

ture gradient is being established a certain portion of the resultant stress is relieved by plastic flow. Thus, the final stress developed in the rod upon cooling at a uniform rate to room temperature is dependent upon the amount which was removed at the higher temperature shortly after cooling started.

10. From the foregoing, it is apparent that in order to have a stress-free casting after heat treatment, stresses of sufficient magnitude must be present during cooling from the stress relieving temperature and the proper cycle for any particular steel depends upon its proportional limit. In other words in cooling castings for maximum stress relief, it is necessary to heat to a sufficiently high temperature to remove stresses rapidly, cool to some temperature at which stress relief is comparatively slow, iron out the temperature and stress gradient at this point, and then cool in the manner
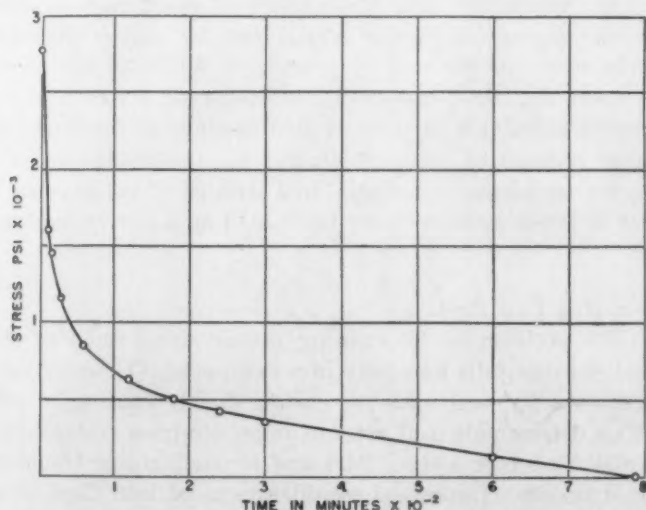

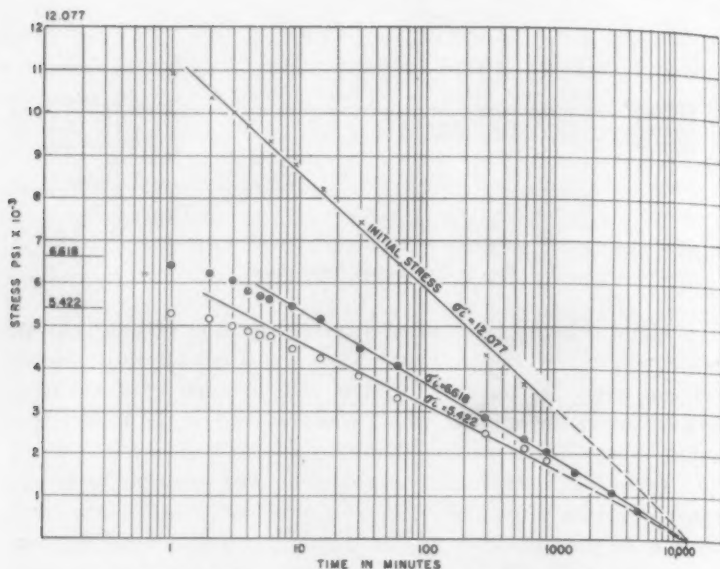FIG. 8—TYPICAL RELAXATION CURVE FOR PLAIN CARBON STEEL AT 650°C.

FIG. 9—RELAXATION OF PLAIN CARBON STEEL AT 550°C.

illustrated in Fig. 2. Adams and Williamson[1] have shown that while the cases described above were concerned with cooling at a uniform rate, "........ a little consideration will show that the explanation may be extended to other types of cooling and that in general *the strain [or stress] remaining is equal and opposite in sign to the reverse strain [or stress] lost by viscous yielding in the early stages of the cooling process.*" While all the above is based upon the ideal case and conditions of stress would not be as easily calculable for complicated castings as for round bars, the same phenomena do prevail and an understanding of the underlying mechanism is essential to a solution of the problem. The removal of stress in steel "may be treated as a purely mechanical process."

Problem Has Two Parts

11.   The problem of determining proper stress relieving cycles for steel castings falls naturally into two parts: (1) an evaluation of the stresses imposed upon the casting during heating or cooling and (2) a determination of rates of relief of stress (relaxation) as a function of temperature. Part one is particularly troublesome in that it involves theoretical considerations of heat flow, proportional limits of steels at elevated temperatures and elastic and

plastic deformation, as well as practical measurements to prove the validity of the theoretical developments. This is now being studied and will be treated in future papers. It is believed that the method described herein satisfactorily solves the requirements of part two.

KNOWN FACTS AND THEORETICAL CONSIDERATIONS

12. It has been generally supposed that internal-stress is relieved at a rate which is proportional to the stress and corresponding equations have been developed. As early as 1866, Clark Maxwell[1, 3] suspected that this phenomenon did not always apply. The results of Adams and Williamson[1] on glass and our results for steel prove that his misgivings were correct.

13. In Fig. 4, three sets of measurements for cast plain carbon steel at 550°C. (1022°F.), 650°C. (1202°F.) and 750°C. (1382°F.) are plotted with σ (stress) as ordinate and t (time in minutes) as abscissa. The data are plotted in this manner to show that the rate of release of stress is not proportional to the initial stress, as indicated by the fact that the curve passing through the points is
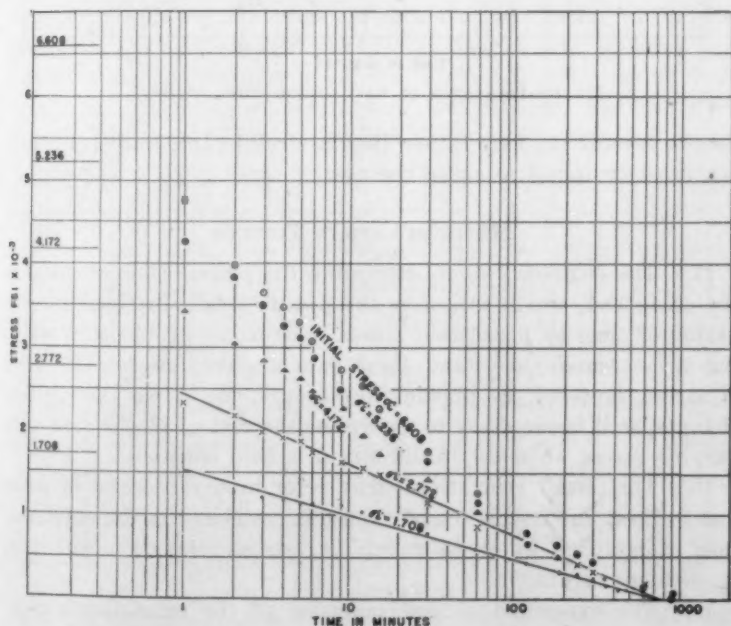

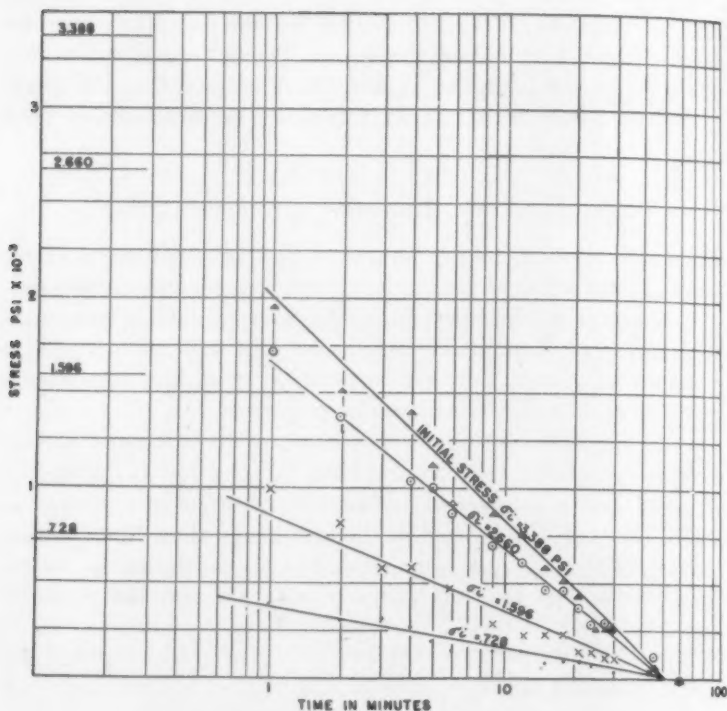FIG. 10—RELAXATION OF PLAIN CARBON STEEL AT 650°C.

FIG. 11—RELAXATION OF CAST CARBON STEEL AT 750°C.

convex toward the axis rather than a straight line and that a new law must be found to cover the case.

METHODS USED IN TESTING

14.   The common test to determine the plastic flow of metal is the creep test, which measures the plastic strain produced over a period of time by a constant stress. For many problems in which the stress remains constant, these data are very useful. In relief of stress, however, as plastic flow occurs, the stress distribution changes, so it is necessary to determine the rate of plastic flow with varying stress when the initial strain is held constant.

15.   The term "relaxation" may refer to any decrease of stress due to flow, but in this report its use is restricted to the particular case exemplified by stress relief, i.e., stress relaxation with time at constant strain.

16.   The experimental determination of the relaxation rate of steel at stress relieving temperatures requires special apparatus. A

search of the literature on the subject revealed an article[4] describing a machine designed by A. Nadai and J. Boyd of the Westinghouse Research Laboratories. Although designed and used for an entirely different purpose, it was apparent that the instrument could be used for measuring rates of relief of stress in steel. Accordingly arrangements were made for its use by Naval Research Laboratory personnel and the data comprising this report were taken at the Westinghouse Research Laboratories in East Pittsburgh.

17. The details of this instrument were supplied to the Naval Research Laboratory and a similar one has been constructed here embodying the fundamental principles, but modified to fit the particular requirements of stress relief testing. This will be used to investigate other steels in the same manner shown herein for plain carbon steel. Since the relaxation machines at Westinghouse and Naval Research Laboratory are identical in principle, it will suffice to describe the operation of the Naval Research Laboratory unit to indicate how the original tests were made.

Description of Machine

18. The machine is so constructed that a very slight change in length of the test specimen controls a motor driving the loading mechanism. This decreases the load in such a way that the gage length is kept practically constant. Figure 5 is a diagrammatic sketch and Fig. 6 is a photograph of the machine. The test specimen C (Fig. 5) is loaded through a lever system A by the springs
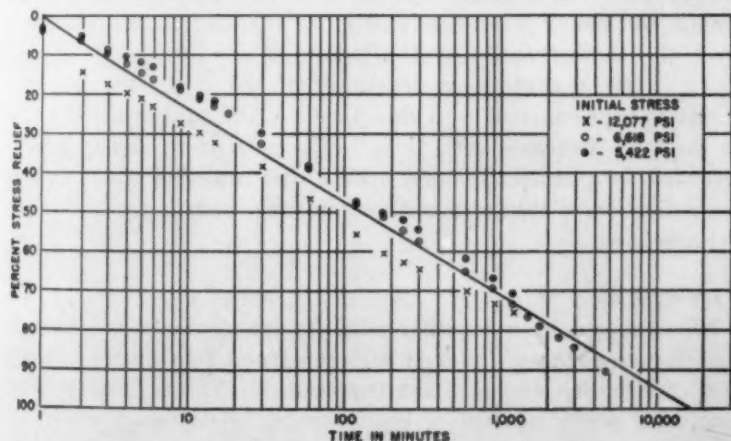

FIG. 12—PER CENT STRESS RELIEF IN PLAIN CARBON STEEL AT 550°C. PLOTTED AGAINST TIME.
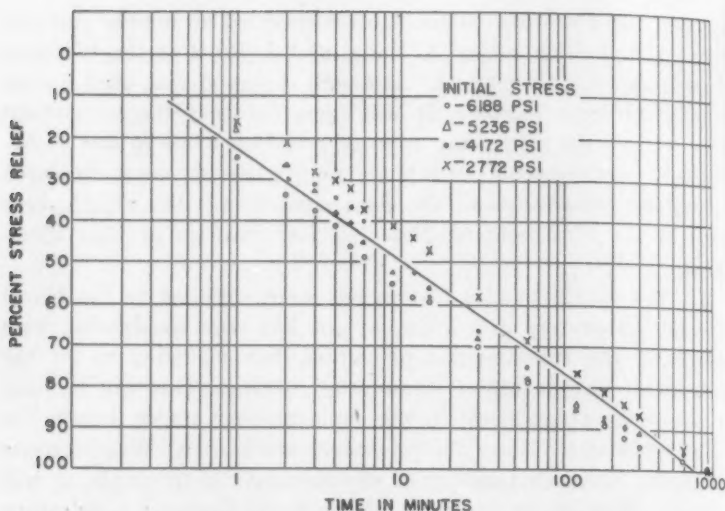
FIG. 13—PER CENT STRESS RELIEF IN PLAIN CARBON STEEL AT 650°C. PLOTTED AGAINST
TIME.

I which are driven by a worm gear drive *G* connected to the motor
H. The test specimen is 0.357-in. in diameter with a gage length
of 6-in. and threaded ends, the details of which are given in Fig.
7. Any errors introduced by the operating parts of the machine
are eliminated by attaching the extensometer *E*, which operates
the sensitive micrometer contact *F*, directly to the specimen.

19. As the specimen lengthens by creep, contact *F* closes the
circuit and motor *H* releases enough of the load on the springs to
open the contact and stop the motor. According to Dr. Nadai'
"...... By a continuous repetition of the cycle, the specimen
length is kept constant to within 1 or 2 x 10⁻⁶ in. per in. of strain
as the load decreases with time." The rate of release of load is
recorded by a camera focused upon a dial indicator which follows
the deflection of the springs, these, in turn, measuring the load on
the specimen.

Heating Unit
20. The specimen together with its extensometer is heated in
the electric furnace designed and constructed to insure uniform
heat distribution and constant temperature. The furnace is 20-in.
long, thus providing 7-in. of heated length beyond each end of the
6-in. gage length of the test specimen. The heating element consists
of three units with external taps for individual regulation. A

nickel tube serves both as a heating chamber and as a means of temperature control by the relative difference in expansion between it and a quartz rod. This operates an electronic relay connected so as to interrupt part of the furnace current. By this means the temperature can be kept constant to $\pm$ ½°C. (0.9°F.) to $\pm$ ¼°C. (0.45°F.) with respect to time and to $\pm$ ½°C. (0.9°F.) along the gage length and is practically independent of changes in room temperature.

21. In all tests, the test specimen together with its extensometer is placed in position and allowed to heat slowly with the furnace; at least one hour is allowed after the specimen reaches the test temperature before the load is applied.

Data Obtained

22. Tests were confined to plain carbon (Navy Class B) steel as this material constitutes the major part of the steel castings produced in this country. The steel was melted in a 300 lb. basic-lined induction furnace from S.A.E. 1015 melting stock and poured into test coupons from which the relaxation test pieces were cut after normalizing. Ferrosilicon, ferromanganese, specially prepared 4.5 per cent iron-carbon alloy and aluminum were used as the additions. The composition of the test material was carbon = 0.24 per cent; manganese = 0.59 per cent; silicon = 0.49 per cent; sulphur = 0.049 per cent; and phosphorus = 0.023 per cent.

23. In Table 1 will be found the mechanical properties of the material at the various testing temperatures. These were experimentally determined in a suitable furnace with a special extensometer attachment reading to 0.00005 in. per in.

Table 1

Mechanical Properties of Normalized Test Material

Composition, per cent

C.	Mn.	Si.	S.	P.
0.24	0.59	0.49	0.049	0.023

	Temperature			
	32°F.	*1020F.*	*1200°F.*	*1380°F.*
	20°C.	*550°C.*	*650°C.*	*750°C.*
Proportional Limit, lb. per sq. in.	27,000	11,000	5,500	3,300
Tensile Strength, lb. per sq. in.	65,500	38,200	19,100	11,420
Elongation, per cent	28.9	36.3	49.8	82.7
Reduction of Area, per cent	52.9	63.6	76.0	89.0

24.   Tables 2, 3, and 4 show the results of the series of measure-
ments obtained from the relaxation tests over a range of tempera-
ture and stress so chosen as to approximate the present stress-
relieving treatments. The tests covered a range of stress from 5,-
422 to 12,077 psi. at 550°C. (1022°F.), 1,708 to 6,605 psi. at 650°C.
(1202°F.) and 728 to 3,388 psi. at 750°C. (1382°F.). These ranges
were so chosen as to bracket the proportional limit of the steel at
each temperature to determine how stresses of these relative magni-
tudes affected the rate of relaxation. With each set of observations
is shown the temperature Θ; the elapsed time, t, in minutes; and
the corresponding values of σ, the stress in psi.

Table 2

STRESS RELAXATION OF PLAIN CARBON STEEL AT 550°C. (1020°F.)

$$t_o = 13,500 \text{ min.}$$
$$b = 0.2435$$

Time in Minutes	Stress in lb. per sq. in.		
0	12,077	6,618	5,422
1	10,881	6,394	5,235
2	10,320	6,207	5,122
4	9,983	6,020	4,936
5	9,497	5,646	4,786
6	9,310	5,609	4,711
9	8,749	5,422	4,412
12	8,488	5,272	4,300
15	8,151	5,122	4,225
20	7,403	4,449	3,799
60	6,431	4,076	3,301
120	5,347	3,472	2,860
180	4,786	3,187	2,703
240	4,524	3,016	2,604
300	4,300	2,817	2,504
600	3,627	2,305	2,106
900	3,253	2,020	1,824
1200	2,954	1,764	1,594
1500		1,551	
1800		1,394	
2400		1,195	
3000		1,053	
4800		640	

25.   The method of calculating and the significance of the fac-
tor t_o appearing at the top of each table, is discussed later in this
report.

Table 3

STRESS RELAXATION OF PLAIN CARBON STEEL AT 650°C. (1200°F.)

$$t_o = 792 \text{ min.}$$
$$b = 0.2627$$

Time in Minutes	Stress in lb. per sq. in.				
0	6,608	6,188	5,236	4,172	2,772
1	4,788	4,648	4,284	3,416	2,324
2	3,976	4,088	3,836	3,052	2,184
3	3,640	3,836	3,500	2,856	1,988
4	3,472	3,556	3,220	2,744	1,932
5	3,308	3,332	3,108	2,632	1,876
6	3,080	3,164	2,856	2,492	1,726
9	2,716	2,772	2,492	2,268	1,624
12	2,520	2,576	2,436	1,960	1,540
15	2,268	2,492	2,296	1,736	1,456
30	1,745	1,848	1,639	1,405	1,149
60	1,256	1,344	1,149	1,022	851
120	788	924	766	660	639
180	639	672	617	511	511
240	553	476	532	422	426
300	468	336	468	405	362
600	128		170	213	128
900			64	0	0
1200			0		

GENERAL DISCUSSION

26. A typical relaxation curve is shown in Fig. 8 where the stress relaxation data are plotted on ordinary linear stress-time coordinates. It will be noticed that during the first portion of the test, relaxation is very rapid. Although this is the ordinary form of expressing the results, inferences drawn from certain theoretical considerations, as well as the need of compressing the time scale, lead to plotting almost all the data semi-logarithmically.

27. In Figs. 9, 10, and 11 are plotted the relaxation data for plain carbon steel at 550°C. (1020°F.), 650°C. (1200°F.) and 750°C. (1380°F.) with σ as ordinate and t as abscissa. The value of the initial stress is indicated on each curve.

28. Since the stress decreases less rapidly for small amounts of initial stress than would correspond to commonly accepted equations, it was thought that perhaps the logarithmic law might satisfactorily represent the conditions (rate of release of stress). Accordingly, the equation

$$-\frac{d\sigma}{dt} = mt^{-1}$$

was tried in which t denotes the time and σ the stress. Integrating this equation and applying the condition that whem $\sigma = 0$, $t = t_o$ the following equation is obtained

$$\sigma = -\sigma_o \, log\frac{t}{t_o} \qquad (1)$$

in which σ_o is the value in psi. on the stress scale equal to one cycle on the time scale and t_o is a constant. The quantity t_o is a measure of the time at which the initial stress σ_i is completely released when the temperature remains unchanged and will be referred to as the stress relieving time for 100 per cent stress relief.

Table 4

STRESS RELAXATION OF PLAIN CARBON STEEL AT 750°C. (1380°F.)

$$t_o = 50 \text{ min.}$$
$$b = 0.3187$$

Time in Minutes	Stress in lb. per sq. in.			
0	3,388	2,660	1,596	728
1	1,960	1,708	980	336
2	1,512	1,372	812	308
3	1,484	1,176	588	308
4	1,400	1,036	588	252
5	1,120	1,008	476	196
6	924	868	476	140
9	868	700	280	140
12	756	616	224	140
15	588	448	224	84
18	504	448	224	84
21	448	364	140	84
24	252	280	140	56
27	252	280	112	56
30	252	252	112	28
45	84	112	28	28
60	0	0	0	0

29. This equation (1) was found to represent the relation between stress and time satisfactorily. When the relaxation data are plotted semi-logarithmically as was done in Figs. 9, 10, and 11, a straight line passing through the points confirms this. No attempt was made to check the validity of the above formula at tem-

peratures below that covered by the experiments and many more
tests will be required before it can be considered a law. It can be
said, however, that within the temperature range considered, which
is of practical importance for stress relief of steel, this formula
gives a useful and sufficiently accurate representation of the facts.

30. Although equation (1) satisfactorily represents the data,
it is inconvenient for calculation and especially for substitution
into the thermal stress equations. A much better formula was
derived based on the ratio of the remanent stress to the initial
stress, that is,

$$\frac{\sigma}{\sigma_i} = - b \, log \, \frac{t}{t_o} \tag{2}$$

in which σ_i is the initial stress and b a constant. The quantity b is
a measure of the rate at which the stress ratio decreases when the
temperature remains unchanged and has the value on the stress
ratio scale equal to one time cycle.

31. Using equation (2) and subtracting the ratio from unity,
the following expression for per cent stress relief is obtained

$$Per \; Cent \; Stress \; Relief = \left(1.00 + b \, log\frac{t}{t_o} \right) x \; 100 \tag{2a}$$

in which b is the stress relieving constant as determined from equa-
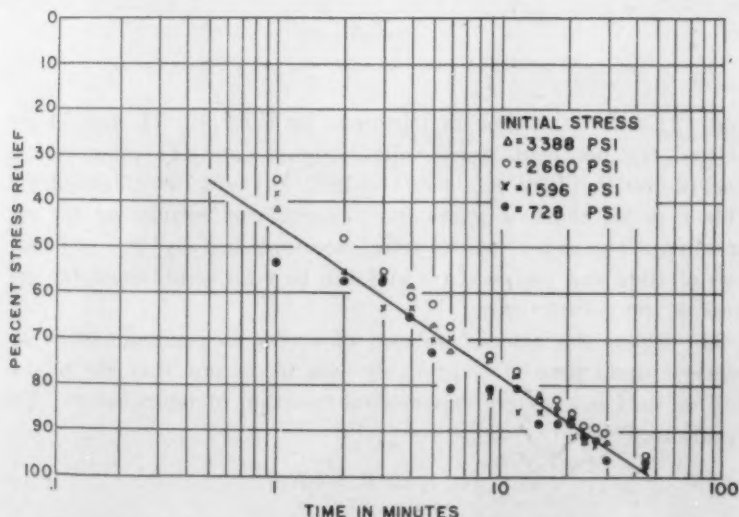

FIG. 14—PER CENT STRESS RELIEF IN PLAIN CARBON STEEL AT 750°C. PLOTTED AGAINST
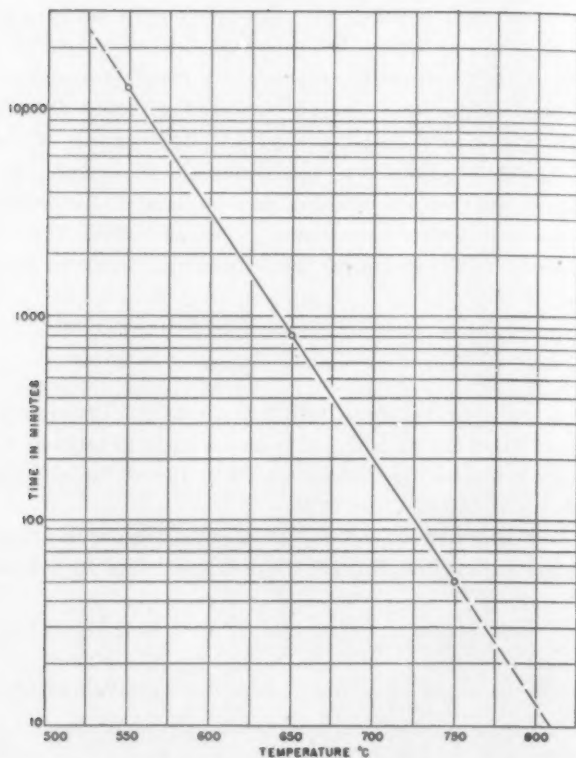TIME.

FIG. 15—TIME-TEMPERATURE RELATION FOR 100% STRESS RELIEF.

tion (2) and t the time in minutes. In Figs. 12, 13, and 14 are
plotted the per cent stress relief against time for plain carbon
steel at 550°C. (1022°F.), 650°C. (1202°F.) and 750°C. (1382°F.)
This type of chart is particularly useful for estimating the ap-
proximate amount of stress relief accomplished by any combina-
tion of time and temperature and can be readily obtained for any
steel in the manner given.

32. Since the rate of release of stress is predominantly de-
pendent upon time, it is probably safe to assume that the relaxa-
tion of steel is a simple exponential function of temperature. The
equation :

$$log\ t_o = R_1 - R_2 \theta$$

in which R_1 and R_2 are positive constants, expresses with sufficient
accuracy the variation of the stress relieving constant over the

range of temperatures tested in our experiments. In Fig. 15, log t_s is plotted against Θ which represents temperature in degrees Centigrade. The straight line drawn through the points for plain carbon steel determines the constants R_1 and R_2, which, when known, make it possible to determine stress relieving times for a steel from two or more observations upon the particular steel at each of two or more temperatures.

ACKNOWLEDGMENT

33. Grateful acknowledgment is made herein to H. Scott for bringing the work of Adams and Williamson to our attention and to Dr. A. Nadai for council during development of the work; to E. Davis, J. Boyd, and R. E. Morey for helpful suggestions and for assistance in conducting the tests; to the Westinghouse Research Laboratories, East Pittsburgh, for allowing us to use their apparatus and for furnishing details of their machine; also, to the Navy Department for sponsoring the work and for permission to publish the results.

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DISCUSSION

Presiding: H. H. BLOSJO, Minneapolis Electric Steel Casting Co., Minneapolis, Minnesota.

Co-Chairman: A. H. JAMESON, Malleable Iron Fittings Co., Branford, Conn.

J. S. VANICK[1]: Where does this stress occur on cooling? I assume that it is temperature gradient caused by the difference in heat flow at the surface as compared to the heat flow of the body.

MR. ROMINSKI: When a piece which is stress-free at a high temperature starts to cool, the surface contracts. This contraction is resisted by the inside, which maintains a greater length than the surface because of its higher temperature. This resistance to free contraction will place the outside in tension and the inside in compression, and the stresses will be relieved almost instantaneously if the piece is at the high temperature. Now, as cooling continues, keeping the rate of cooling constant, the surface will finally reach room temperature, while the inside is possibly 50 or 75 degrees hotter. When the inside continues to cool from this point, it contracts, and this contraction, now being resisted by the outside, will set up final residual stresses in the casting. The stresses were all re-established after the outside assumed a constant temperature. If the cooling is stopped at an intermediate temperature, for example 500°C., and held there long enough to remove the temperature gradient, the stresses will be re-established. If it is held a sufficient length of time, some of the residual stresses will be removed by relaxation. The cooling can then begin so that at the initial stage the material is strong enough to resist any flow and will hold the stresses set up by the temperature gradient.

MR. VANICK: Does that apply to a casting with a sufficiently large section to create within it this 50 or 75 degree temperature gradient?

MR. ROMINSKI: Wherever there is a temperature gradient, there is also a stress gradient. If there is a difference of only 2 degrees in temperature, there will be a difference in length equivalent to those 2 degrees.

MR. VANICK: If the practical foundryman is cooling a casting in a furnace, at the rate of, say, 30 degrees an hour, and that temperature gradient is very much less than 50 or 75 degrees maximum, say, a 25 degree maximum, do you compute as residual stress the 25 degree difference between the surface at room temperature and the center as it affects the expansion?

MR. ROMINSKI: Yes, providing all the stresses were lost when cooling was started. If all the stresses were not lost, then only part of them would be re-established. The residual stresses that are re-established are always equal and opposite to those lost in the initial stage of cooling.

MR. VANICK: How slow a rate would be necessary for different sections of castings to eliminate the residual?

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[1] International Nickel Co., New York, N. Y.

Mr. Rominski: We do not now have enough data to formulate a complete answer. If the rate of cooling is slow, the temperature gradient is small and the resulting stress is low. Moreover, the slow rate of cooling allows a longer time for the relaxation of the stress at any temperature and a larger percentage of the stress will be lost. With a fast cooling rate the temperature gradient will be larger and the corresponding stress higher. However, the time at any temperature is less and a small percentage of the stress will be lost through relaxation. Now, the problem, which is greater—a large per cent of a small stress or a small per cent of a large stress?—is a problem on the rate of cooling which is being investigated at the present time.

There are two problems in stress relieving: First, the removal of residual stress at the stress relieving temperature, and second, the cooling of the piece to room temperature. These two parts are diametrically opposite. In the first part, the stress is to be removed as fast as possible, and in the second part, all the stress is to be retained in the piece. At the present time we are trying to develop a compromise, such as cooling to an intermediate temperature, removing the temperature gradient and some of the residual stresses established due to the loss of stress at the higher temperature, then cooling so that all the stress is retained until room temperature is reached, when the temperature gradient and the corresponding stress gradient are removed.

G. A. Timmons[2]: Is it not necessary to have a plastic deformation on cooling in order to have any residual stress left at all?

Mr. Rominski: Yes, it is necessary to have plastic deformation in order to have residual stress. The plastic flow that takes place during relaxation is creep. The relaxation test differs from the creep test, however, in the respect that the initial strain is held constant and the decrease in stress measured instead of holding the initial stress constant and measuring the plastic strain. It might help to illustrate the mechanism of relaxation and the part that creep plays by the use of a stress-strain diagram as shown in Fig. 16.

The test specimen is strained to a predetermined value. This is shown on the stress-strain diagram (Fig. 16) as A. The specimen begins to creep at constant stress until a strain of 2×10^{-6} in. per in. greater than the initial strain is reached (point B). At this time some of the load is removed and the specimen contracts elastically back to its initial value (point C). Creep again takes place, but at a reduced stress and a slower rate until the increment of strain is again reached, when more of the load is removed and the specimen contracts elastically to its initial value. This cycle is repeated indefinitely until all the load has been removed.

Mr. Timmons: In cooling a heavy casting, when the outside is contracting this puts the inside in compression and the outside in tension. If the stresses are reduced at high temperature, some plastic deformation is required. In cooling something like a 4-in. cube with a temperature gradient, the outside is trying to contract, and is, therefore, in ten-

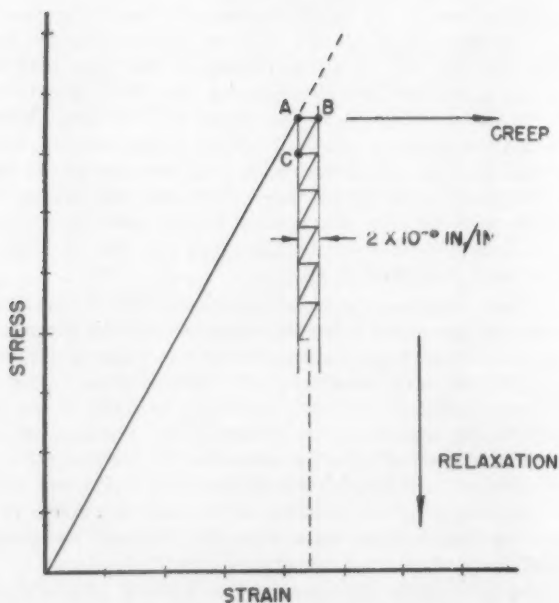[2] Climax Molybdenum Co., Detroit, Michigan.

FIG. 16—STRESS-STRAIN DIAGRAM.

sion, while the inside is in compression. Where is the plastic deformation obtained that will result in a residual stress when thermal equilibrium is attained at room temperature?

MR. ROMINSKI: In relaxation or the disappearance of stress at temperature.

MR. TIMMONS: Does that entail a change in volume or an increase in density?

MR. ROMINSKI: I believe not.

MR. TIMMONS: But the outside is contracting which tends to contract the whole mass, and yet the inside resists this contraction and is thus under compression. If the inside does not move or change in density, is it not going to be just as it was when brought down to room temperature?

MR. ROMINSKI: It will be the same; that is, stress free, when the piece is brought to room temperature, providing none of the temporary thermal stress is lost. If any of the temporary thermal stress is lost by relaxation it must be accounted for when the piece assumes a constant temperature.

If the stress at any time during the cooling exceeds the proportional limit at that temperature and plastic deformation takes place as permanent strain, it is another problem. In such a case, a change in length must be accounted for when the piece is brought to the original conditions of uniform temperature.

MR. TIMMONS: Is there a change in dimension?

MR. ROMINSKI: No, the initial strain remains constant.

W. H. PARKER[3]: In a large range of castings, consisting of all kinds of sections from, say, 12-in. to ¾-in., do you think it is possible, with the tools that the foundryman has, to entirely stress relieve them?

MR. ROMINSKI: Yes, it is possible to stress relieve them 100 per cent. This is shown very clearly in Fig. 15, which gives the time-temperature relation for 100 per cent stress relief of plain carbon steel. However, with that difference in section it would be difficult, if not impossible, to cool the casting from the stress relieving temperature to room temperature without re-establishing some stresses. It would also require a careful analysis to make certain that the temporary thermal stress caused by the temperature gradient does not exceed the proportional limit. By careful analysis of the casting and thermal conditions, a stress relieving cycle can be planned for any casting that will result in a minimum amount of residual stress.

CHAIRMAN BLOSJO: Even though there is no change in length of the total specimen, is it not necessary to have plastic deformation to equalize the stresses and stress relieving?

MR. ROMINSKI: Yes, believe it or not, we have plastic deformation without any change in length or volume. However, there is nothing strange about this when we realize that the stress is held elastically, and it is this elastic deformation that is converted into plastic deformation.

T. E. NORMAN[4]: Can you give us any idea of the nature of this relaxation? Apparently the plastic deformation which occurs under high stress allows for the relief of that stress. Is it related to creep or is it some other form?

MR. ROMINSKI: It is creep.

MR. NORMAN: It would appear from that that it is impossible to have stress relief without going up to temperatures at which creep occurs.

MR. ROMINSKI: That is true. Within the temperatures of the work done, between 550 and 750°C., for every 25 degrees of change in temperature, the time is either increased or reduced by a factor of 2. For example, it will take twice as long if the temperature is reduced 25 degrees.

Tests of Pattern Coating Substitutes for Shellac

By Frank C. Cech*, Cleveland, Ohio

1. This report has been prepared to supplement a paper[1] presented to the Patternmaking Division of A.F.A. at the 1940 Convention. The findings of the paper were based on a national survey of shellac and varnish manufacturers, pattern supply houses, pattern manufacturers and foundries.

2. Indirectly, the survey revealed that:

   (1) Varnish manufacturers had given but little thought to special pattern coatings, as they knew little of the specifications for a good pattern coating and even less of the potential market waiting for them. However, when properly informed of the facts, they were eager to cooperate in both experimentation and production of new coatings.

   (2) Pattern supply houses found pattern manufacturers relatively unwilling to try new coatings.

   (3) Pattern manufacturers were content to use the traditional coating—shellac. They were skeptical of new coatings. Consequently, it was foreseen that it would take an acute shellac shortage, or decided price rise, or both, to get them to give new coatings a fair trial. The first World War presented such an opportunity, but it failed to produce a satisfactory shellac substitute. In general, the coatings which were developed were too long in drying and gummy to sandpaper.

3. Since the 1940 survey, several new coatings have made their appearance. This report presents the results obtained from tests made with available samples. The results presented are not of a laboratory nature but rather of a practical nature. The tests were devised to learn those properties of the coatings in which a pattern shop or foundry is most interested. Further, the results were ob-

tained under circumstances and conditions found in the average pattern shop or foundry. A hypothetical case follows:

A pattern is wanted in a hurry so that a casting may be produced in as short a time as possible. Upon its completion, a protective coating is applied. The pattern then is watched impatiently for the time when it is dry enough to sandpaper smooth and apply a filler to nail holes and blemishes. This being done, a second coat is applied and again it is watched for sufficient dryness, this time so that it may be placed in the sand for molding.

Upon being placed in the mold, there follow two more practical tests. The one to see whether the sand will adhere to the coated pattern, and the other to see how long the coating will withstand the constant abrasion in drawing the pattern. These are the practical tests which must follow laboratory tests of the manufacturers.

Coating No. 1

4. Pollard Oil Products Co., 1627 S. 44th St., Milwaukee, issued a circular describing a pattern coating which the company manufactures. However, no trial samples were available so there is no report.

Coating No. 2

5. The Great Lakes Varnish Works, Inc., 2207-35 N. Crawford Ave., Chicago, manufactures a shellac substitute under a trade name. This coating brushes freely, is readily absorbed and dried hard enough to sandpaper after the first application, in 30 min. It was slightly tacky to touch at that time. A second coat was applied immediately after sanding and, after 40 min. of drying time, was placed in the mold. No adhesion of sand was discovered upon removal. Drawing the pattern left a smooth mold. After 50 molds, the pattern coating showed no more wear than shellac.

Coating No. 3

6. The Kindt-Collins Co., 12653 Elmwood Ave., Cleveland, also has been marketing a shellac substitute under a trade name. An outstanding feature of this coating is the vividness of the red and chrome-yellow colors. The contrast immediately accentuates the color markings, as suggested by the A.F.A. standards. The coating rolls over the pattern surface during brushing. Upon brush-

ing, it leaves furrows, much like enamel, which disappear during the drying process. It is absorbed less readily, but dries to allow sandpapering within 10 min. after application. After sanding, the second coat dried to touch in 30 min., at which time it was placed in the sand. Withdrawal of the pattern left a smooth mold with no sand sticking to the pattern. Fifty molds from the pattern left no more abrasion wear than shown by shellac.

Coating No. 4

7.   The McDougall-Butler Co., Inc., Buffalo, also has put a pattern coating on the market. It brushes freely, is absorbed readily and dries to touch in 15 min., after which it permits sanding. A second coat dried in 45 min., following which the pattern was molded. Drawing the pattern left a smooth mold. The pattern showed no signs of adhering sand. A run of 50 molds left no more signs of wear, due to sand abrasion, than would be found on shellacked patterns.

Coating No. 5

8.   Foundry Rubber Compounds Corp., 1050—30th St. N. W., Washington, has on the market a pattern coating that is a radical departure from shellac. The rapidity of absorption and drying quality is much greater than that of shellac, drying to touch in less than 5 min. The coating dries with a smooth surface not unlike that left by graphite. Molds, after the withdrawal of the pattern, are smooth. There is no sand sticking to the pattern. The wear on the coating compared favorably with shellac. It seemed that three coats would be necessary to give the coating a body comparable to the other pattern coatings.

9.   The foregoing pattern coatings are worthy of trial, as shown by the short-run test results. However, final choice rests with the consumer. No pattern equipment will suffer damage because any of the above coatings have been used. It is this fact that allows a fair trial of the coatings.

DISCUSSION

Presiding: VAUGHAN REID, City Pattern Works, Detroit, Mich.

A. H. TRAPP[1]: Mr. Chairman, I am sure that a lot of these gentlemen and their patternmakers are confronted with a situation similar to mine. About 2½ yr. ago we installed a sand-throwing machine in our plant, and one of the first problems was to convert our patterns quickly to something that could be employed with the machine. We had been using shellac exclusively on wood patterns, but it was unsuited for the new device.

Our foundry superintendent suggested using aluminum powder in the shellac, instead of ordinary lampblack or black-pattern extract. We had resorted to the pattern extract by that time, with marvelous results from the aluminum powder instead of black-pattern extract. We continued that way, but it was not long before we were seeking something that would give harder coatings as the basis for the shellac.

We tried a crankcase sealer, which is the same kind of material in which we dip our castings after they come from the foundry, to keep them from rusting before storage in the yard. Originally, the sealer was furnished to us in a yellow color, which was very nice for coating core prints. I decided that, perhaps, I could use the crankcase sealer in a natural color, mixing it with pattern extract or lampblack. However, I soon discovered that it was not suitable because the lampblack contained too much oil. After an interlude, when we used a red sealer which had a much harder body, we learned that the same product was obtainable in a natural color. We started using this, mixed with aluminum powder, and we feel that we have conquered the pattern finishing problem.

We have not used shellac for quite some time. We are using the crankcase sealer in the red color for the ground coat and mixed with the aluminum powder for the finishing coats. I would say that our results are 100 per cent satisfactory.

CHAIRMAN REID: For how many castings will you use a wood pattern?

MR. TRAPP: We will produce 100 to 150 molds from one pattern under the new machine before we will give them another coat. We have some hardwood patterns that have produced over 3000 molds under the slinger.

MR. CECH: When you speak of aluminum powder, that was the highest-grade pattern coat coloring matter that we tried in 1940. However, I was under the impression that aluminum powder was difficult to get, which is the reason I did not mention it, even though it is the most efficient of colorings.

MEMBER: Have you had any experience with lacquer as a coating? Our trouble with pattern coatings is that we use hot sand, and the shellac and other coatings stick.

CHAIRMAN REID: Mr. Kindt, in view of your past experience with this problem, you should be able to give us the answer.

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[1] Minneapolis Moline Power Imp. Co., Minneapolis, Minn.

E. T. KINDT[2]: That sticking problem is true of all pattern coatings. The various coatings have a definite melting point, just as shellac has a definite melting point. I believe that lacquer is the greatest offender in that respect because it gts soft when hot sand is applied. Consequently, the sand sticks to it and it will not withdraw properly. The pattern coating, of which I have a sample here, also has a definite melting point, and I can guarantee that it is a much higher melting point than that of shellac.

Shellac is not, to my way of thinking, the ideal pattern coating. Contrary to public opinion, shellac is not even waterproof or moisture-proof. Does that answer your question?

MEMBER: No, the shellac sticks. When we applied the lacquer to new wood, the lacquer did not stick—it is harder. However, we tried to put it on an old pattern but we could not do it successfully.

MR. KINDT: You are not supposed to put lacquer over shellac. If you are going to apply hot sand over a pattern coating, you must have a high melting point material; otherwise, it is naturally going to soften and the sand will adhere to it. I have no particular reason to condemn lacquer but, from past experience, I do not believe it is a suitable pattern coating.

MR. CECH (author's closure): The reports of the pattern coating tests are encouraging to say the least. First, they bear out the findings of the survey that the manufacturers of coatings are willing to continue experimentation to find newer and more satisfactory coatings. Second, that a continuous alertness on the part of some agency willing to try out samples will help to produce a better coating. Third, that an association, such as the American Foundrymen's Association, is needed, not only because it is in a position to bring before the users of pattern coatings the latest coatings available, but also because it is forever on the alert to preserve reliability of statements made in regard to all matters involved in its technical meetings.

The discussion brought from the floor should convince all present that the subject of pattern coatings is as live today as in the past. It is hoped that those attending the technical meetings, or hearing of the proceedings, will try the newer materials available and send their findings to the A.F.A. for publication in the *American Foundryman* so that all may benefit from them.

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[2] Kindt-Collins Co., Cleveland, Ohio.

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Use of Cement in Foundry Molding

By C. A. Sleicher*, Providence, R. I.

Abstract

The principles and reactions in the use of cement in foundry molding are explained. Reasons are given for the value of cement in molding, some of which are lower material costs, savings in labor, fuel and power, higher casting quality, etc. The author discusses the problems involved in the use of cement and gives the proper mixture, method of mixing and drying time for cement molds, stressing the importance of the mold blacking operation.

1. In presenting this paper to the Association, it is done with the hope that it will, in a simple and elementary way, give some thoughts and suggestions which may be of help in our present-day problems.

2. Many good foundrymen take pride in the fact that they are hard-boiled and are not given to trying equipment or processes which may be of help in their problems, usually for the reason that they are too busy or set in their ideas to give the time to the study or trial of new ideas. In a way, they are not entirely to blame, for if they listened to all the ideas advanced to foundrymen, they would have little time for their real work. However, there is a place where we can all afford, especially in these times, to stop, look and listen.

Principles and Reactions in the Use of Cement

3. The underlying principle of the cement process is that the sand and cement are sub-hydrated. In other words, even with excessive water the chemical reaction is never completed. A film of the hydrated cement sets up a wall of protection for the cement which is inside, preventing further chemical reaction.

FIG. 1—OUTSIDE FACE OF WHEELS MADE WITH CEMENT CORES.

This chemical reaction is rather complex and rather hard to express in a chemical formula, as we have

			Cement		
SiO_2,	Fe_2O_3,	Al_2O_3,	CaO,	MgO,	SO_3,
Silicon	Ferric	Aluminum	Calcium	Magnesium	Sulphur
Dioxide	Oxide	Oxide	Oxide	Oxide	TriOxide

all entering into the reaction with the water.

4. Thus we can see when we mix silica sand, cement and water together and either coat or just furnish enough cement on the sand grain for contact of grains, that we eventually get a hard mass by various degrees of hydration. First the initial hydration takes place due primarily to evaporation and enough temperature, about 70°F., to cause hydration. Above this temperature, evaporation may hinder hydration, and, below it, especially as we get to 40°F., hydration stops. The principle here is that proper temperature is needed for a chemical reaction.

5. There is quite a difference in principle involved between the ordinary dry sand process and the setting up of the cement mold. In the latter we have a definite chemical reaction. To quote from an article in *Machinery*, July, 1937, ''All natural, as well as artificial, bonds, in general, used for molding sands, have a fusing point considerably below that of the sand itself, and also below the pouring temperature of the molten metal. Naturally, when the metal comes in contact with the bonds, they disintegrate into their

component gases, and getting rid of these gases is one of the problems in foundry practice. The making of molds of cement, sand and water alone has overcome this difficulty. Although cement has a somewhat lower fusing point than sand, there are practically no gases generated when the molten metal comes in contact with the cement."

REASONS FOR VALUE OF CEMENT IN MOLDING

Lower Material Costs

6. Cement is relatively a cheap product. It is generally conceded that the use of cement as a binder is cheaper than oil, pitch, flour, etc. The saving, of course, depends upon the quantity of binder used and the price. The big saving comes in the re-use of the cement bonded sand. The chunks of this material are generally ground in a mill and re-used. In one large railroad foundry, a regular roller-type mill was used. The fine sand resulting was run through a screen and binned for re-use with a fixed proportion of new sand. The pieces left which had been reduced to egg size were separately binned and used as back up material for the molds.

Increased Production Under Limited Baking Capacity

7. Cement can be used anywhere where the iron shrinks away from the mold or core. Thus, for instance, in the molding of large or medium size castings which are made up largely of cores (such

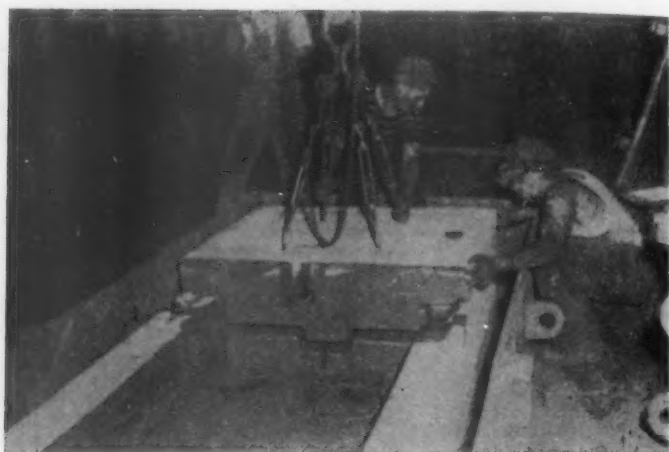FIG. 2—LATHE BED PAN MOLD. SIDES MADE OF CEMENT CORES.

FIG. 3—LATHE BED PAN COVER MADE WITH CEMENT CORES.

as lathe bed castings, etc.) all of the side, bottom and cover cores may be made of cement. The inside cores, against which shrinkage is set up, should be conventional type cores in order to avoid shrinkage strains which would lead to cracks.

8. The latter statement is subject to debate. Claims are made of the successful use of inside cores, and inside cores are used with success. However, in our practice, we have refrained from their use in general. It is our belief that there is little, if any, contraction to the cement core when the iron shrinks around it, with the result that a strain or weakness may be set up in the casting itself which, even if it does not actually show up by visible cracks, is, nevertheless, there and may appear either in machining or in service.

Saving of Fuel

9. The cost of fuel is a large item in foundries which largely operate on a dry sand basis. Today we have the additional problems of getting oil and fuel created by the war. No fuel is needed in drying cement molds. Therefore, the fuel cost is in proportion to the use of cement in molding and cores in the foundry.

Saving in Ordinary Foundry Labor

10. If the labor involved in moving large cores into the oven and in moving dry sand molds to the oven could be eliminated, the average foundryman would be surprised at the saving which

would result in labor costs, to say nothing of the effect on the loss sometimes incurred through damaged cores and molds which must either be repaired or thrown out. This labor item is a large one, and involves, in addition, considerable losses which are rarely considered, due to the hold-up in molding and other work while moving molds and cores to the ovens or away to floors where they are to be cored and poured. Some foundries do this at night or on overtime which naturally adds to the labor cost.

Saving of Power

11. Power is a big factor in costs. It requires a good deal of power to run cranes up and down a large foundry and move molds and cores to and from the ovens. Also, there is the power needed to operate the blowers, etc., of the ovens, which is eliminated when cement is used. Cement molds and cores can be made on the floor where they are to be finally poured, and all of the power factor is thereby eliminated.

Less Skilled Labor Involved

12. There is little ramming required in the use of cement. If rammed with an ordinary air rammer, the cement is more liable to move from one place to another and the effect of the ramming is thereby lost. Also, ramming, if effective, would have a tendency to produce a less permeable mold. Cement molds have a high permeability. The cement can be, to a considerable degree, shoveled

FIG. 4—LATHE BED PAN COVER CORE. TOP MADE OF CEMENT. LOWER PART BOLTED ON SAND.

FIG. 5—CEMENT CORE IN THE MAKING TURNED OUT ON WOODEN BOARD.

in and tamped lightly with a large faced hand tamper. This process, while not as fast as a sand slinger or bumper, nevertheless, is rapid and reduces the molding time very considerably. It is easy to cut molding costs well in half in making cement molds in comparison with ordinary ramming practice.

Elimination of Flask Equipment

13. In some cases, where medium sized castings are made in sand, no flasks are used. For instance, cylindrical shapes such as liners, etc., can be set up entirely in cores made in sections around which iron hands are wedged to hold them together. Molds so made are more easily shaken out and a considerable amount is saved in flask equipment.

Drying Equipment Saved

14. We all realize the problems and cost of core racks, core plates, etc. These can largely be eliminated where cement is used. Substantial wooden planed boards perforated with holes can be used instead of core plates.

15. The side cores for lathe beds, etc., and all similar cores, can be laid out, in most cases, on wooden planed boards, similar to the ordinary bottom board. They will be dry enough the day following manufacture to turn off the board and handle and stack with a crane. They can be stacked by simply placing sticks or two-by-fours between to permit the air to circulate. In this manner, the

space required to manufacture cement cores is not great, and racks for drying and stacking are unnecessary.

Denser Castings Are Produced

16. In cement castings, it would appear that the iron is somewhat closer as if pressure is put upon it. This may be due to the fact that a cement mold evidently does not expand in like manner to a sand mold. This is a theory which is subject to investigation, but we have found that cement mold castings seem to be more leakproof than the sand mold castings where iron conditions are the same.

17. One of the leading metallurgists of our country said: "I'm sure of one thing—it will give you density in castings, especially light sections where solidification will be speeded up."

18. Blow-holes are eliminated to a larger degree, as cement molds are very permeable, allowing steam and gas to escape faster. Also, the amount of gas produced is less, due to the fact that gas producing binders are not used.

Higher Quality Castings Are Produced

19. Cement molds, in the main, are harder and less liable to abrasion or spalling than a sand mold. The walls and edges stand up firmly and the hot iron does not cut the mold. Because of the added density and generally greater freedom from dirt and blowholes, less finish may be added to castings made in cement.

FIG. 6—MACHINE TOOL CASTING MOLD WITH ROUND END CORE MADE OF CEMENT.

FIG. 7—SHOWING CORE ILLUSTRATED IN FIG. 6 AFTER POURING.

Snagging Time Cut

20. It has been claimed that the time required to clean and chip castings made in cement is reduced at least 50 per cent. This is no doubt true. Often a cement casting made in a pit will come out for the most part with simply the blacked face. There will be sharper edges and fewer bumps and bumps due to mold defects, as is often the case with sand molds. It can be readily seen that cleaning costs can be greatly reduced.

PROBLEMS INVOLVED IN THE USE OF CEMENT

Materials Used and Method of Mixing

21. The best sand is a washed silica sand of about 40 grain fineness. In order to get a quick and thorough mix, the sand and cement should be run dry for approximately 2 min. in an ordinary mixer. For small quantities, an ordinary mixer works very satisfactorily. For larger quantities a roller pan type mill may be used economically. After the mixture has been run dry for the above time, the water may be added and the batch run for about another minute. This will ordinarily give a good mix.

22. In producing the proper mixture, the following proportions of silica sand, cement and water work satisfactorily:

89 per cent white silica sand, 40 grain fineness
11 per cent quick drying cement

To this total add 6 per cent water by volume. It is rather impor-

tant to be accurate with the water volume, as excessive water leads to many troubles.

Drying Time

23. Molds should be allowed to dry for 72 hours. This applies to large and small molds alike. This time may vary somewhat, due to temperature and humidity. Seventy-two hours will give, on large molds and cores, sufficient depth of drying to use. A dry of one-in. depth in large molds is sufficient.

Making the Mold

24. In making pit molds, etc., where at times the pattern is left overnight in the sand, changes in pattern designs are necessary, for, in most cases, patterns must be drawn within the day period. If left in overnight, the pattern becomes set in the cement. This trouble is easily overcome by various methods. Instead of loose pieces, cement cores are made and pinned in place when the cement mold is made. Loose sides are made a part of the pattern so that they can be removed easily and the pattern reprinted and wedged into place where more than one day is required for the molding operation, in producing large castings.

25. As an illustration of how simply and easily pattern problems are worked out, the following is a good example: In manufacturing a large engine frame as shown in Fig. 9 there were several problems involved. The casting weighed several tons and was
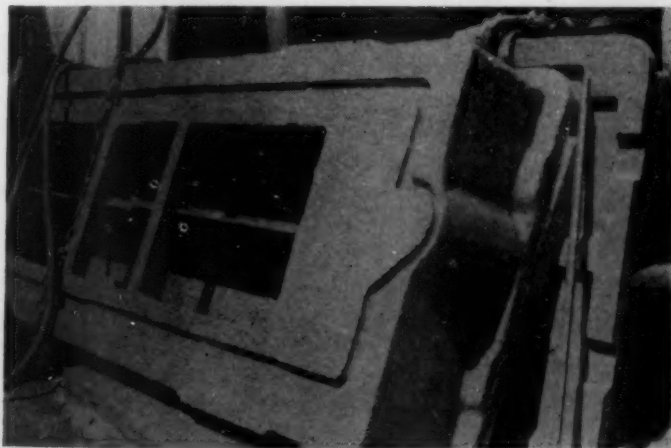

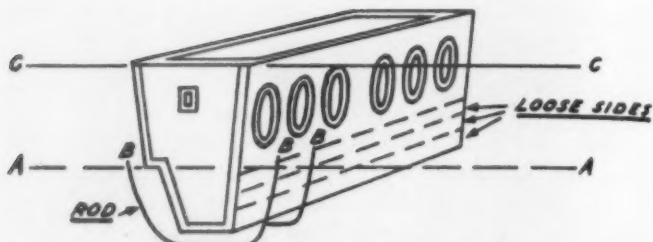FIG. 8—LATHE BED PAN CASTING SHOWING SHARP EDGES.

FIG. 9—DESIGN OF LARGE ENGINE FRAME.

made in a pit. The approximate depth of the pit was 6 or 7 ft., and the length about 14 ft. First, a coke bed was laid, over which was laid a cement bed of about 8-in. The pattern was set on this and cement shovelled and tamped around the pattern to point A-A. The side thickness of cement was approximately 8-in. which was reinforced with rods and backed up by refuse cement and sand which was also tamped in. It was found advisable to bend rods around the bottom of the mold as shown at B-B, to cushion the bottom mold strain. These were placed about 12-in. apart. The pattern was then drawn. The board sides, as shown by the dotted lines, were removed from the pattern, and the following morning the pattern was easily reprinted and wedged in place and completed to the face C-C.

26.   The cope was flat, and was rammed on a level concrete floor laid out and made especially for that purpose. This is a simple method of handling a situation when more than one day is necessary to make a mold.

27.   After the mold has dried one night, it is sufficiently hard to walk over and coat with blacking. Before blacking, if there are any repairs needed, such as a broken corner or a damaged face, these repairs are made. To do this, a little glue or paste is added to the regular cement mixture, and formed in the place to be mended. It will dry quickly and be stronger than the mold itself.

Blacking the Mold

28.   What may seem a minor operation becomes an important factor with cement. The mold or core may be blacked the day following manufacture. *One coat is not enough.* At least two coats, and sometimes three, are necessary. The cement works well with three coats, despite some beliefs to the contrary, and, where the blacking is well done, the cement will drop off with comparative ease, when the mold is shaken out, leaving little cleaning neces-

sary. If not properly blacked, the iron imbeds itself in the grains of sand and creates a casting which cannot ever be properly cleaned, as the sand grains become imbedded in the face of the casting itself.

CONCLUSION

29. Cement molding presents few problems to the foundryman. With a little thought given to pattern design and a little ingenuity, these problems are easily solved. There is little difference between the shrinkage of a casting made in a cement mold and one made in a dry sand mold. If there is any difference at all, it will appear that the cement mold probably shrinks a trifle less. Alloy castings present no additional problems, as they cast satisfactorily in cement molds.

30. Cement has real possibilities in the foundry for those who will study and experiment with it and can be of invaluable help now when capacities are strained to the limit.

ACKNOWLEDGMENT

The Effect of Heat Treatment on Physical Properties of Some Common Irons Used in Oil Field Equipment

By Ernest R. Starkweather*, Lufkin, Texas

Abstract

The author describes research conducted on the effect of heat treatment on the physical properties of four grades of iron used in oil field equipment, giving data on quenching temperatures and tensile tests. He discusses test results with the aid of charts and micrographs.

1.   Due tribute should be paid to the steel industry for the very extensive data available on the heat treatment of their product. Be it straight carbon, or alloy steel, a person with decidedly limited experience can, through the medium of reasonable care and proper equipment, duplicate, fairly accurately, the results obtained in the well-equipped experimental and research laboratories. This is due, mainly, to the availability of charts, tables, etc., giving the critical points, quenching media, proper drawing temperatures, etc., to produce, ultimately, the desired physical properties.

2.   It is to be regretted that the same information is not available in the cast iron field. It cannot be denied that the average gray iron casting is not subjected to heat treatment in the true sense of the word. Likewise, the complicated structure of the iron itself may have some bearing on the matter. Furthermore, there was a time not so many years ago when cast iron was just cast iron, and the industry, generally speaking, was not too progressive. It must be conceded, however, that, during recent years, tremendous strides have been made toward a better metal and no one need be ashamed of the product now offered to the world at large.

3.   In possibly too hasty a perusal of available information pertaining to the heat treatment of cast iron, three publications[1, 2, 3]

rendered the most assistance and yielded valuable data for which the writer is grateful. Likewise, Bartholomew[4] mentions the interrupted, or hot quench, wherein the strength of an iron was increased from 47,500 to 75,500 lb. per sq. in. This is also known as "austempering" and consists of heating to 1400 to 1600°F. and quenching in a hot liquid held at some pre-determined temperature such as 400, 600, or 800°F. The piece is held in the quenching bath long enough to obtain the desired results. Obviously, this is an attractive feature where time is a vital factor, in that it eliminates one operation of the standard "quench and draw" technique.

HEAT TREATMENT OF CAST IRON

4. Gray cast iron, in many instances, is as responsive to heat treatment as is steel. With a view of obtaining actual data with respect to the effect of heat treatment on some of the iron poured in our foundry, the laboratory conducted the necessary research work to enable us to arrive at some fairly accurate conclusions.

5. In order that this work may be pictured with reasonable clarity, the following points should be mentioned:

Table 1

PROPERTIES OF LIGHT QUENCHING OIL

Specific Gravity, Baume'	0.9042
Flash Point, min.	320°F.
Fire Point, min.	350°F.
Viscosity @ 100°F. (Seybolt Universal)	100-110
Specific Heat (approx.)	0.50
Carbon Residue	0.01
Saponification No.	0.10

Table 2

CHEMICAL ANALYSIS

Grade	Silicon, per cent	Sulphur, per cent	Phosphorus, per cent	Manganese, per cent	Total Carbon, per cent	Comb. Carbon, per cent
I	1.29	0.126	0.19	0.74	3.24	1.02
II	1.45	0.148	0.26	0.56	3.18	0.99
III	1.59	0.128	0.35	0.60	3.17	0.85
IV	2.07	0.150	0.45	0.45	3.28	1.04

[4] Bartholomew, E. L., "A New Heat Treatment of Cast Iron," STEEL, August 5, 1940.

(1) The quenching temperatures in every instance ran from 1550 to 1600°F., with light quenching oil as the medium except as otherwise indicated. The soaking period was one hour for both quench and draw operations, both conducted on the original 1.20-in. diameter bar. Properties of the quenching oil used are shown in Table 1.

(2) All tensiles were machined from the standard arbitration, Grade B, *i.e.*, 1.20-in. diameter, and 21-in. long. This bar was then sawed into several specimens about 4-in. long and machined in accordance with the A.S.T.M. standard 0.800-in. diameter tensile specimen. Drillings were taken from the end of the bar, thus insuring uniform analyses.

TEST RESULTS

6. Too much emphasis cannot be made relative to the metal involved, and its inherent brittleness. While we have used the latest type of vertical hydraulic testing machine, we have to admit discrepancies in the various results, for although, theoretically, a direct pull was supposedly given all bars, the slightest mis-alignment materially affected the tensile results. Likewise, inconsistencies of machining, even microscopic, as well as fillets minutely imperfect, all tended to cause fluctuations of the true ultimate strength.
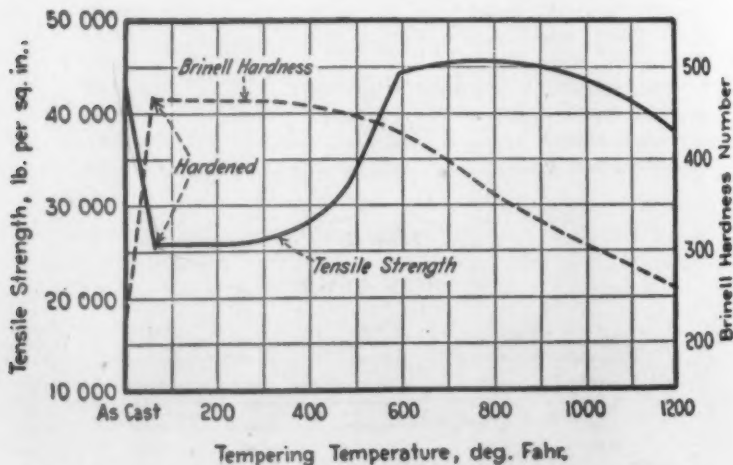

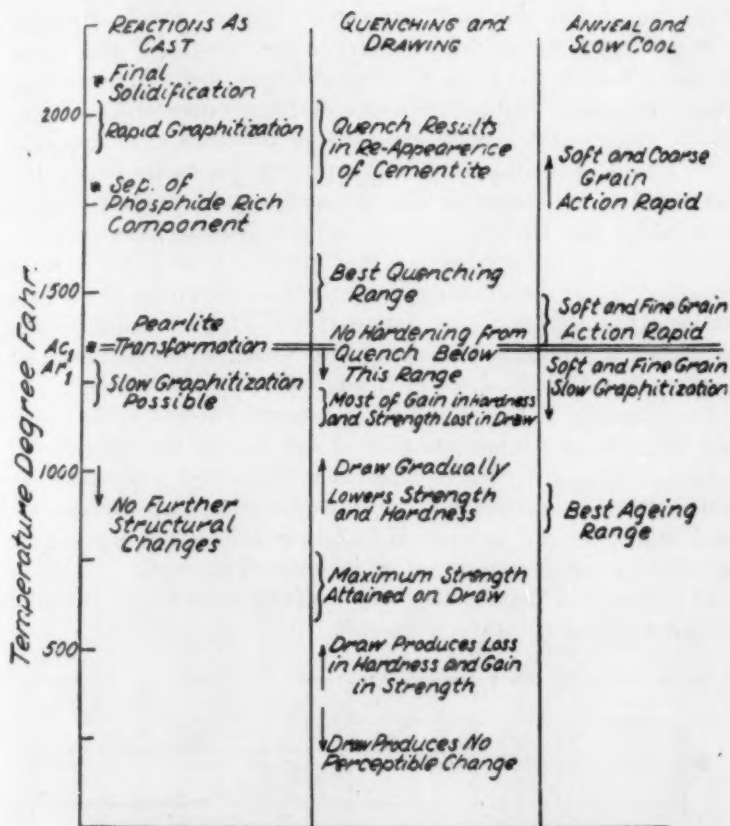FIG. 1—EFFECTS OF TEMPERING TEMPERATURES ON THE PROPERTIES OF CAST IRON.

FIG. 2—EFFECTS OF VARIOUS HEAT TREATMENTS ON GRAY IRON.

7. The irons showed an increase in strength over the as cast bars from slightly under 20 per cent to about 40 per cent. This is not to be regarded as phenomenal, but is indicative of what can be accomplished with ordinary cupola metal.

8. From a chemical viewpoint (Table 2), the irons embodied in this paper are not at all unusual and can be duplicated in any foundry. They represent the major portion of the castings eventually assembled into pumping units distributed widely throughout the oil fields. Although certain essential parts are alloyed, it was considered advisable to restrict this paper to plain gray iron.

9. Figure 1[2] possesses a certain amount of interest in that the Brinells corresponded, fairly closely, with Grades II and III. How-

ever, the author obtained appreciably higher tensile strengths.

10.   Figure 2[1] is quite instructive to the "amateur" heat treater of plain cast iron. Here is very concisely depicted many vital phases such as critical points, best quenching range, and just where maximum physicals can be expected on the drawing temperature.

11.   Table 3 is, in reality, a synopsis of this entire paper. It is particularly interesting to note the almost mathematical precision with which the Brinells increase as the drawing temperature is lowered. It is to be regretted that all grades of metal were not subjected to all the drawing temperatures. However, if the hardest metal, as cast, had been drawn at 800°F., machinability might have been a problem. Likewise, there would be no particular advantage in a 1200°F. draw of Grade IV, our weakest metal.

12.   Figures 3, 4, 5, 6, and 7 represent Table 3 broken down, and the various components plotted one against the other.

13.   In Fig. 3 it is to be noted that the softest iron shows very little difference in tensile in the three drawing temperatures. This same statement can be applied to Grade III, although this grade shows the greatest percentage of increase of strength.

14.   Figure 4 depicts the very uniform increase in Brinell as the drawing temperature is lowered.
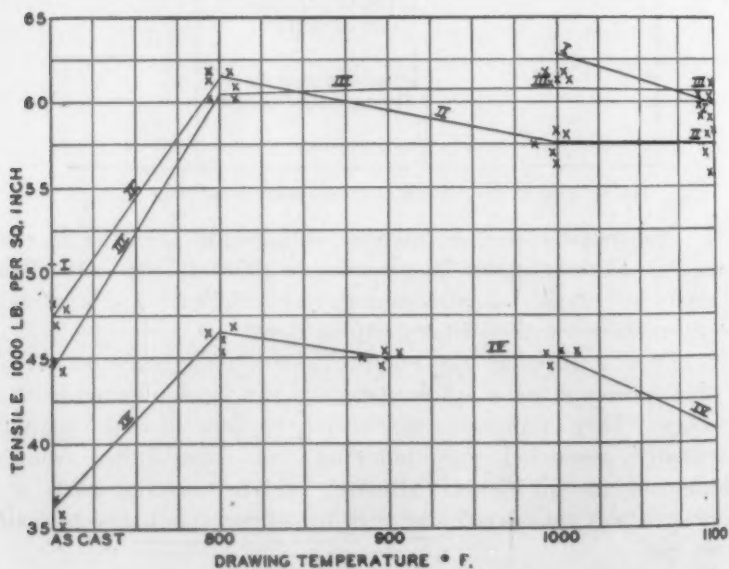

FIG. 3—SHOWING TENSILE STRENGTH PLOTTED AGAINST DRAWING TEMPERATURE FOR 4 GRADES OF IRON TESTED.

Table 3

HEAT TREATED BARS

Grade	As Cast Tensile	As Cast Brinell	Brinell After Quench Oil	Brinell After Quench Air	1100 Brinell	1100 Tensile, lb. per sq. in.	1100 Increase Over As Cast, per cent	1000 Brinell	1000 Tensile, lb. per sq. in.	1000 Increase Over As Cast, per cent
										Drawing Temperature, °F.
I	50500	255	444–455		269	59400	17.6	311	63500	25.7
II	47700	248	477–495		255–277	57500*	20.5	302–311	57400	20.3
III	44000	241	461–495	241–248	255–269	60800	38.1	286–302	60900*	38.4
IV	36200	235	388–477	183–187	269			241–255	45000*	24.3

Drawing Temperature, °F.

Grade	900 Brinell	900 Tensile, lb. per sq. in.	900 Increase Over As Cast, per cent	800 Brinell	800 Tensile, lb. per sq. in.	800 Increase Over As Cast, per cent	Air Quench Brinell	Air Quench Tensile, lb. per sq. in.	Air Quench Increase Over As Cast, per cent
I									
II				341–363	61500*	28.9			
III	321–341	61500*	39.5	341–363	60800	38.1	241–248	55200	27.7
IV	269–302	45000*	24.3	302–341	46600	28.7	183–187	28400	21.5 decrease

* Machined to 0.504-in. bar as original 0.800-in. bar broke in threads.

15. In Fig. 5, the Grade III iron shows about the same percentage of increase in strength at all four drawing temperatures. This, to our mind, is rather surprising.

16. In Fig. 6, a slight increase in hardness over the as cast Brinell resulted in an appreciable increase in tensile. However,
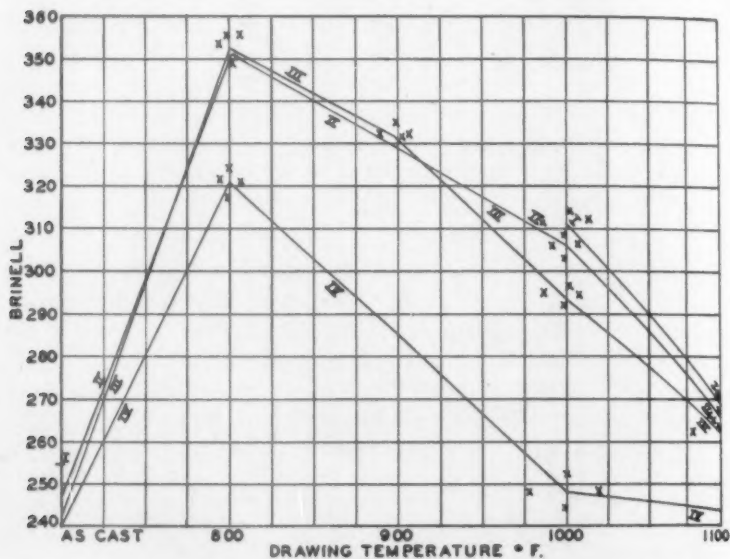

FIG. 4—SHOWING DRAWING TEMPERATURE PLOTTED AGAINST BRINELL HARDNESS FOR 4 GRADES OF IRON TESTED.
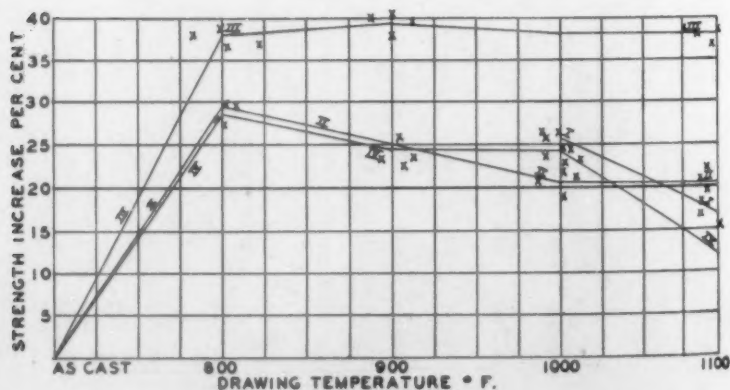

FIG. 5—SHOWING DRAWING TEMPERATURE PLOTTED AGAINST PER CENT INCREASE IN STRENGTH FOR 4 GRADES OF IRON TESTED.
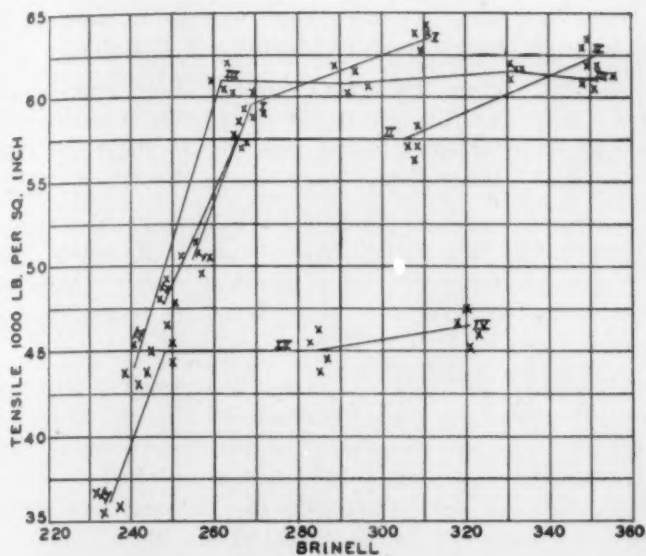
FIG. 6—SHOWING BRINELL HARDNESS PLOTTED AGAINST TENSILE STRENGTH FOR 4 GRADES
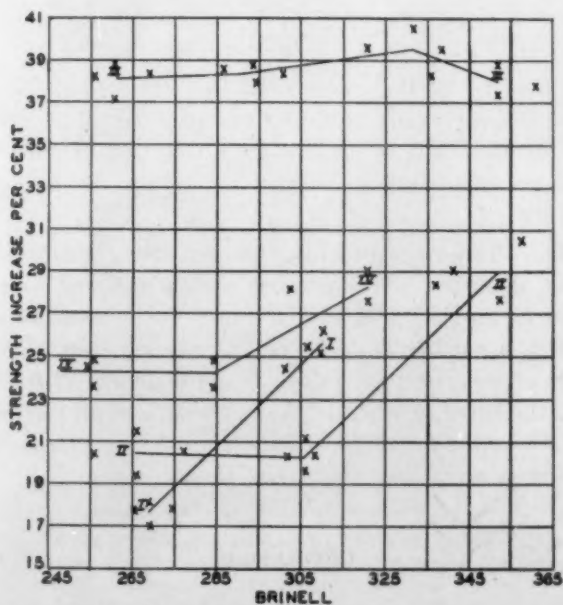OF IRON TESTED.

FIG. 7—SHOWING BRINELL HARDNESS PLOTTED AGAINST PER CENT INCREASE IN STRENGTH
FOR 4 GRADES OF IRON TESTED.

after this initial jump, a much greater increase in hardness is required to bring about any radical increase in strength.

17.   In Fig. 7, we again find Grade III offering almost a straight line, in that the percentage of increase in strength over the as cast bar is about the same from 262 to about 352 Brinell. The other three grades present more logical curves.

18.   Through the medium of the microscope the effect of heat treatment on two of the grades of iron is submitted. Photomicrographs of the hardest and strongest metal, Grade I, are offered in Fig. 8. Likewise, the weakest iron, Grade IV, is pictured in Fig. 9.

19.   While no particular comment is necessary, attention is called to the finer graphitic formation in Fig. 8A, as compared with Fig. 9A. This would naturally be expected in view of the fact that both silicon and carbon are lower in the former specimen. Likewise, standing out prominently is the radically different matrix and structure of the air quenched bar, 9F.

20.   Figures 10A and 10B present a certain amount of interest in that they are micrographs of the two grades of iron which were quenched only, i. e., not drawn. Figure 10A is Grade I while 10B is Grade IV. Both are magnified 500X and etched with "nital."

21.   It is regretted that pictures of castings poured for oil field equipment are not available. All units of this nature are in the field and no more will be made for the duration. Figures 11, 12, 13 and 15 are castings poured from the various grades submitted in this paper.

22.   Figure 11 was poured from Grade I iron. This is an engine liner, 42-in. diameter, 7-ft. high with a 3-in. wall and weighs 6000 lb. Figure 12 represents Grade II iron. It is a 64-in. hoist drum. Figure 13 is a casting poured from Grade III iron. This is the under side of a boring mill table. Dimensions are 12-ft. diameter and 10-in. thickness. Figure 14 shows the largest oil pumping unit ever built, weighing 65,000 lb. Figure 15 represents Grade IV iron. This casting is a leveling plate used in shipyards.

CONCLUSION

23.   Most assuredly, no one can predict with certainty, the iron of tomorrow. Yet, in view of recent progress, probably the day
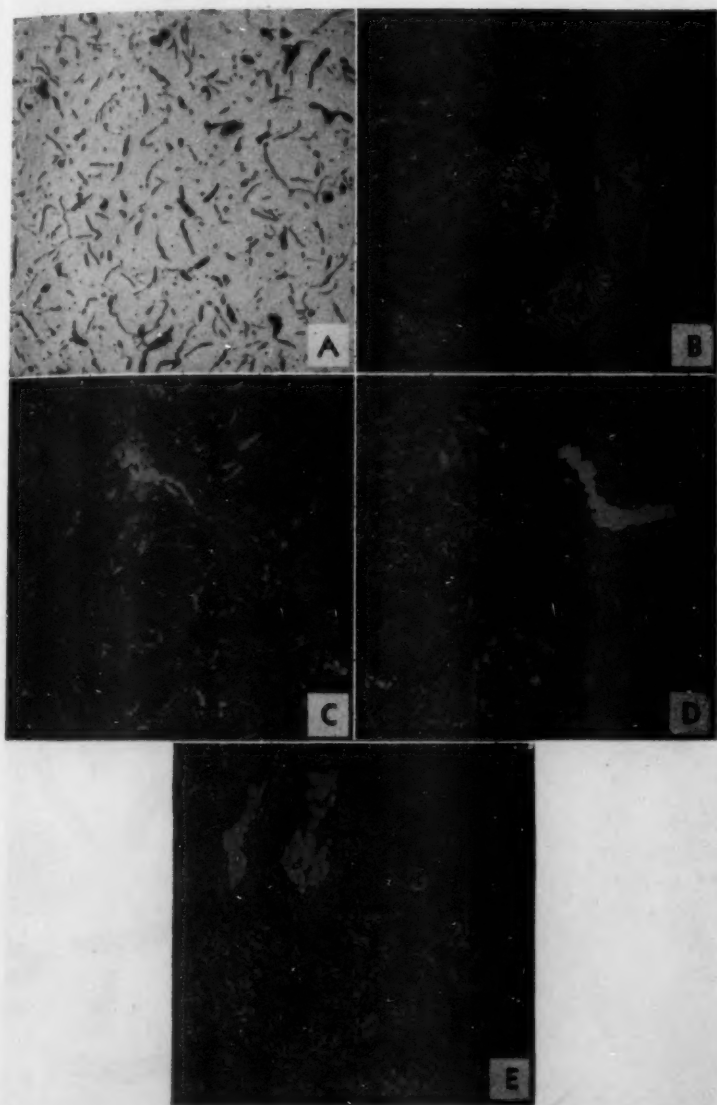
Fig. 8—Effect of Heat Treatment on the Hardest and Strongest Iron, Grade I. (A)—As Cast, Unetched. (B) As Cast, Etched. (C) Drawing Temperature 1000°F., Etched. (D) Drawing Temperature 1100°F., Etched. (E) Drawing Temperature 1200°F., Etched. Magnification (A) x50, (B) Through (E) x500.
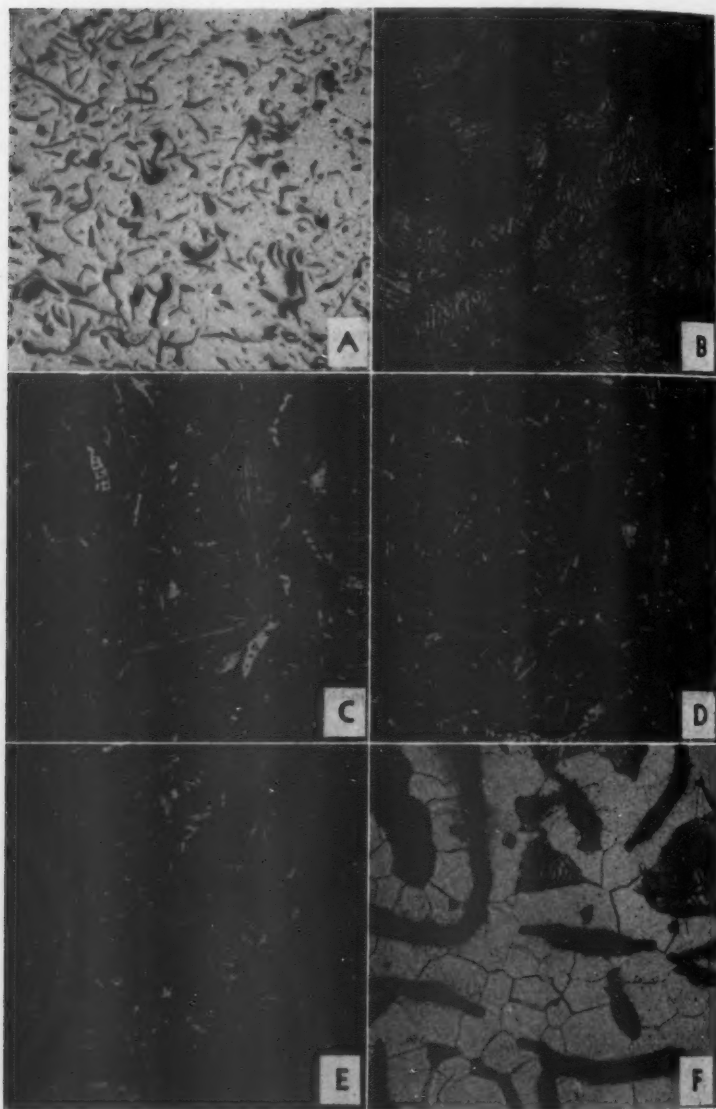
FIG. 9—EFFECT OF HEAT TREATMENT ON THE WEAKEST IRON, GRADE IV. (A) AS CAST, UNETCHED. (B) AS CAST, ETCHED. (C) DRAWING TEMPERATURE 800°F., ETCHED. (D) DRAWING TEMPERATURE 900°F., ETCHED. (E) DRAWING TEMPERATURE 1000°F., ETCHED. (F) AIR QUENCH, ETCHED. MAGNIFICATION (A) x50, (B) THROUGH (F) x500.
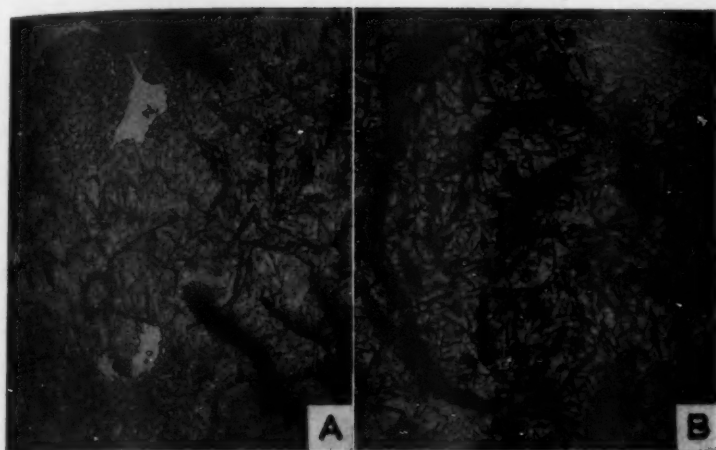
Fig. 10—Effect of Quenching on the Hardest and Strongest Iron, Grade I (A), and the Weakest Iron, Grade IV (B). Etched with Nital. x500.

is not far distant when the boasted iron of the present era will not be acceptable. Due recognition, in time, will probably be given the engineering profession in its ceaseless demand for a better metal, particularly where weight is an essential factor. As a consequence, the metallurgist and foundryman will undoubtedly offer to industry an iron embodying physical properties which, today, are deemed impossible. Who can say but what more research resulting in the improved technique of heat treating of iron may contribute largely to such an advancement.

24. No contention is made that this paper presents any startling results, particularly to those who have conducted similar work. However, if it prompts only a few to delve into this interesting phase of cast iron and its ultimate possibilities, this article will not have been written in vain.

ACKNOWLEDGMENT

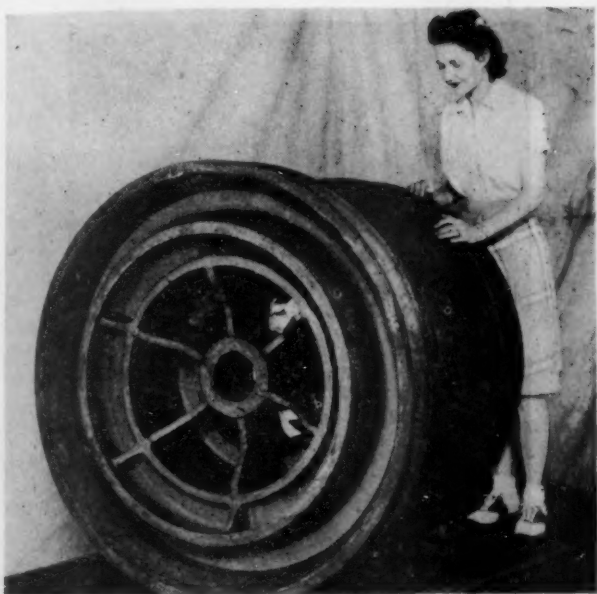FIG. 11—MARINE ENGINE LINER POURED FROM GRADE I IRON.

FIG. 12—HOIST DRUM FOR MARINE SERVICE POURED FROM GRADE II IRON.
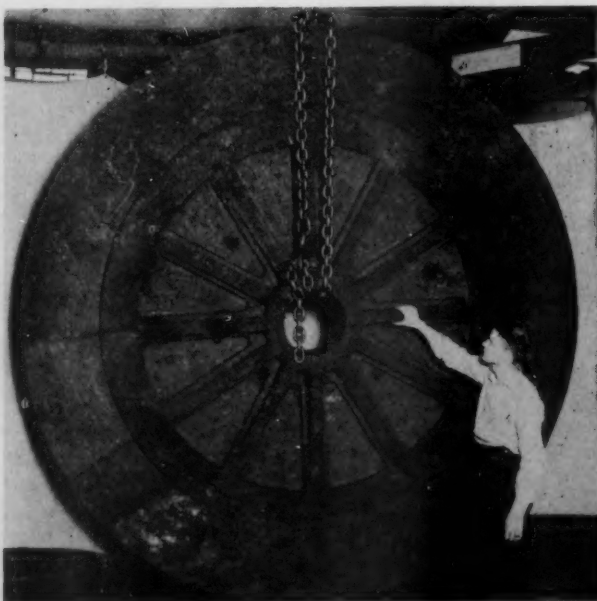

FIG. 13—UNDER SIDE OF BORING MILL TABLE POURED FROM GRADE III IRON.
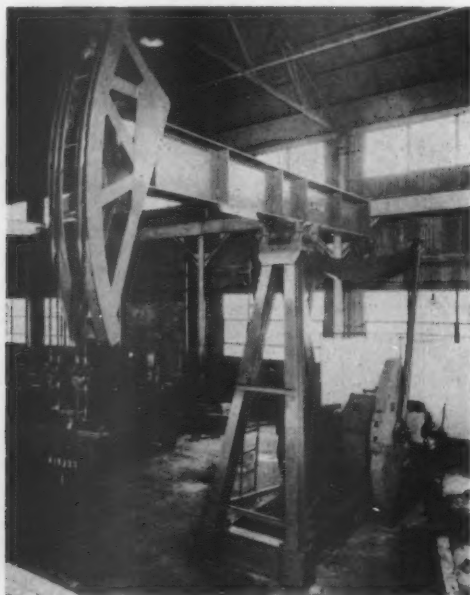
FIG. 14—OIL PUMPING UNIT.

FIG. 15—LEVELING PLATE FOR SHIP YARD POURED FROM GRADE IV IRON.

Conservation of Foundry Refractories

By C. E. Bales*, Ironton, O., and F. McCarthy*, Columbus, O.

Abstract

*The authors state the necessity for giving considera-
tion to the conservation and reclamation of refractory
materials in foundry practice. They cover the right type
of lining for efficient cupola practice and the proper
method of installation, giving recommendations as to the
size and type of blocks. The authors discuss recommenda-
tions for patching cupola lining, touching upon daubing
mixtures. They also discuss in detail the effect of air
supply, charging and slagging on refractories and the
importance of ladle refractories.*

Introduction

1. Fortunately for the foundry industry, and for this nation
in its present war effort, the refractories industry, during the past
20 years, had enough foresight to carry on research work, with the
idea of product improvement and to carry out programs of plant
expansion and modernization. This has made it possible for all
foundries in this country and Canada, to secure an adequate
supply of high grade refractory materials without any undue de-
lays, although the demand for these products has been un-
precedented.

2. If the war continues for any considerable length of time,
there is a distinct possibility that a shortage of refractories may
develop, due to difficulty of securing repair parts for brick plant
equipment, manpower shortages, inability to take time out for
kiln repairs, etc. So far, the War Production Board has granted
high preference ratings to refractories plants and has seen to it
that they secured the necessary equipment and supplies to carry
out the manufacture of these highly essential materials. What the
situation will be a year from now, no one can tell. It, therefore,

* Vice President and Foundry Engineer, respectively. The Ironton Fire Brick Co.

becomes necessary to give some consideration to the conservation and reclamation of refractory materials in foundry practice.

3.   Refractories today are far better both in quality and workmanship than they were in World War I. This is especially true in regard to malleable furnace brick, and it also applies to cupola blocks, as most of them now are made by machine methods which give much harder, denser, tougher blocks. With better refractories, the foundryman *should* be able to obtain longer life from his furnace linings, his costs should be lowered and his castings should be cleaner. The efficient operation of the melting furnaces *will* enable him to secure these desired ends.

4.   The authors believe the cupola is the heart of the foundry. It is one of the oldest furnaces for melting iron for castings, but it is still the most convenient and economical of all, and, therefore, the most widely used. The regrettable thing about cupola operation is that it has remained an art for so many years, instead of becoming a science. Great strides are being made, both in the large production shops and in the progressive smaller foundries, and more scientific knowledge exists today on correct cupola operation, than ever before.

CUPOLA LININGS

5.   One of the most essential things in efficient cupola practice is the right type of lining and the proper method of installing it. There are many types of fire clay available for cupola block manufacture, but the best blocks are made from those clays mined in Kentucky, Missouri, and Pennsylvania. The blocks should be made by the stiff mud, machine-pressed method, as this process gives a much harder, denser block than those molded by hand. The standard 9- x 6- x 4-in. blocks are easily made by the stiff mud method, and it has been our experience that they are the most desirable and economical for general foundry use. Some foundries using very large cupolas prefer 9- x 9- x 4-in. blocks. These, too, can be made mechanically but the Colliau blocks, 9-in. high, giving a 4½-in. lining, are generally always hand-made.

6.   Wherever the size of the cupola will permit it, we favor a double lining of 9- x 6- x 4-in. blocks up to the charging door. This 12-in. lining offers a number of advantages. First, there is a substantial factor of safety. If production demands it, the cupola can be stepped up to its limit without any fear of the lining burning through. It is easier to patch against a solid block lining,

and, since the heat loss will be less, the operator can secure hotter iron and possibly use less coke. Between the lining and the cupola shell, we recommend a clearance of about $\frac{3}{4}$-in. to take care of the expansion of the blocks under heat. This open space can be filled with clay grouting to act as a cushion.

7. During the past year we have found that cupola blocks will last longer in the well and melting zone if they are laid up with super-quality, refractory bonding mortar, instead of ordinary fire clay. This is accounted for by the fact that foundry clay usually has a lower fusion point than the block lining, it shrinks in the joints permitting joint corrosion and it offers very little bonding power to the lining a few inches back of the hot face. High grade, refractory bonding mortars of the proper type have a higher fusion point than the blocks themselves, there is no joint corrosion and the lining is thoroughly bonded together. This is a definite advance in cupola lining practice. We recommend that cupola blocks always be laid with thin close joints.

8. Above the charging door, we recommend the use of 9- x $4\frac{1}{2}$- x $2\frac{1}{2}$-in. circle brick of the proper diameter to fit the shell. These bricks can be laid up in a mortar of ground fire clay. Some foundries use 9-in. straights placed on end to give a $2\frac{1}{2}$-in. lining, while others use a combination of 9-in. straights and arch brick laid on end to give a $4\frac{1}{2}$-in. lining. The circle brick lining is preferred as it gives a much more substantial job and will last indefinitely.

9. After the cupola has been completely lined, it should be thoroughly dried out with a wood or coke fire before running the first heat. This will prevent the hard, dense, machine-made cupola blocks from spalling or flaking off. When the drying operation is completed, the cupola should then be put into operation as soon as possible.

Patching the Cupola Lining

10. After every heat, some patching is necessary in the melting zone and the lining in the well should be carefully examined. Clean walls are the first essential in good relining. Any humps that may have been formed above the tuyeres should be removed. It is difficult to chip off every particle of slag adhering to the burnt lining, but the more nearly complete the job is, the better the patch will be able to protect the lining during the next heat. If the slag is not removed, it will melt and carry the patch down with it. All holes in the lining should be plugged with brick or

refractory patching mud described later. With proper cupola operation the scoring of the brickwork in the melting zone should be even and clearly defined.

11. Those foundries melting high tonnages of hot iron will find it necessary to use 9- x 6- x 4-in. cupola blocks for patching. Smaller foundries can use 9-in. straights, 1¼-in. splits, or refractory daubing mud. Do not throw away the pieces of block removed from the lining as they can be used in making daubing materials.

Daubing Mixtures

12. For many years, we have emphasized the necessity of using good refractory daubing mud and yet still find some foundries which use river sand, molding sand and other low fusing materials for this purpose. It is no surprise that these foundries always complain about not securing satisfactory service from their block linings. Daubing mud should be made from those materials which will actually preserve and protect the lining. Mixtures of fire clay with river sand or molding sand will have a lower fusion point than either of the materials alone. Eutectics are formed in accordance with the principles of thermochemical mineralogy.

13. When such materials are used, the daubing melts down before the cupola reaches its full heat, the patching bricks are carried along with it, the brick lining has no protection, and thick, gummy slags are formed. A serious "bridge" may occur, the slag hole frequently becomes frozen, the melting goes off schedule and the iron may be of inferior quality. The right kind of daubing mud is highly important in conserving refractories.

14. There are nearly as many daubing mixtures as there are gray iron foundries, but the following are recommended, as they give good service and help to preserve the lining:

> *Formula No. 1:* Two parts white silica sand. (Waste sand reclaimed from sand blast machines is excellent for this purpose provided it does not contain too much iron scale.)
>
> One part plastic fire clay. (Fusion point at least Cone 26 [2903°F.].)
>
> *Formula No. 2:* Six parts crushed fire brick, through 10 mesh riddle. (Old fire brick reclaimed from the cupola lining can be crushed by placing in a tumbling barrel containing a few pigs of iron.)
>
> Four parts plastic fire clay. (Fusion point at least Cone 26 [2903°F.].)

15. Either of the above compositions should be mixed dry, then thoroughly blended with enough water to make a stiff plastic mud, preferably in a muller mixer.

16. Never use river sand, molding sand, yellow loam or blue shale in daubing muds.

THE EFFECT OF AIR SUPPLY ON REFRACTORIES

17. We will not attempt to discuss the controversial subject of tuyeres, but it would be impossible to overemphasize the importance of proper air supply. It is essential to see that the right amount of air gets into the cupola with the proper penetration. The blast pressure should be kept uniform and a good pressure gauge will soon pay for itself in increased efficiency. A pressure gauge alone is not sufficient, and it is highly desirable to use a volume gauge or one of the newer air controls to weigh the quantity of air going into the cupola. Excessive blast will oxidize the iron and this will cause severe cutting of the blocks in the melting zone.

18. The life of the melting zone lining is normally proportional to the melting zone temperature and the duration of the heat. It is useless and uneconomical to melt at a higher temperature than is actually required for the type of castings being made. The eyes of most foundrymen, as well as brickmakers, are unreliable and a good optical pyrometer is much better for measuring the temperature of the iron as tapped.

THE EFFECT OF CHARGING ON REFRACTORIES

19. The nature of the mixture used in the cupola and the method of charging has considerable bearing on the life of the lining. A high percentage of steel scrap with its higher melting temperature decreases the life of the brick in the melting zone. The scrap should be in small pieces so that it will not divert the blast against the lining. Careful charging will prolong the life of the lining in the abrasion zone.

THE EFFECT OF SLAGGING ON REFRACTORIES

20. Proper slagging of the cupola is necessary in order to conserve the refractory lining. Very little consideration has been given to the fluxing materials used and little is known regarding their behavior in a cupola. For example, fluorspar is a glassy mineral consisting of 51.1 per cent calcium and 48.9 per cent fluorine. It decrepitates or flies to pieces when heated and it melts at 2462°F.,

forming an enamel like substance consisting principally of lime. It is very useful for thinning slags but is extremely corrosive to the cupola lining if carelessly used.

21. Calcite, the primary mineral constituent of limestone consists of 56 per cent lime and 44 per cent carbon dioxide. Those stones containing an appreciable amount of magnesia (15 to 20 per cent) are considered as dolomite. Aragonite has the same chemical composition as calcite, but it flies to pieces when heated. Most of it may be blown out the stack and it is useless as a fluxing stone.

22. When calcite and dolomite are heated in the cupola they dissociate into carbon dioxide and lime or lime-magnesia mixtures. Dolomite is probably the more effective flux, although limestone is widely used. Whatever stone is used, it should be of high purity and of egg size. In charging, it should be kept about 6-in. away from the cupola lining as lime and magnesia are corrosive to fire brick.

23. Cupola slag in itself is not particularly destructive to the lining unless it contains large amounts of metallic oxides, but the basic materials forming the slag (iron oxide, lime and magnesia) are corrosive. The slag should never be permitted to become stiff and gummy, but should be kept fluid and removed from the cupola as promptly as possible. The slag hole should not be permitted to become so large that the blast blows through it. A blast of slag will destroy slag hole blocks. It is difficult to make a fire clay slag hole block that will give the desired service in large production foundries. Some foundries, however, install two slag blocks side by side. They use one during the first half of a long heat, then mud it off, and complete the slagging operation with the other block. Silicon carbide blocks apparently give good service in large foundries but they are expensive. Worn out snagging wheels, if they are silicate bonded, can be used for slag holes by knocking out the arbor bushing. However, rubber bonded and bakelite bonded wheels are obviously not suitable for this job.

24. Cupola slags are extremely variable in composition, color and physical properties. The easiest slag on the fire brick lining is the one of a grayish green color and hard compact structure. Bluish, foamy slags will indicate that considerable damage has been done to the lining. A careful examination of the slag each day will give much information on the operation of the cupola.

25. Many foundries now use fused soda ash as a supplemental flux in cupolas. This is good practice from the refractory standpoint, even though soda ash would be corrosive to fire brick if

carelessly used. When properly used, soda ash improves combustion, is very effective in forming a fluid slag which can be quickly removed from the cupola, and, since it reduces oxidation, especially where large quantities of steel scrap are used, it actually helps to conserve the life of the lining.

26. The cupola lining can be preserved much longer than usual, provided the last charge is not coked and the blast is shut off as soon as a white heat is reached. More fire brick are "burned" in the cupola at this time than at any other period because the blast works directly on the lining. The blast is not needed after the stock turns white as the bed will melt any small amount of iron that may be left.

27. For cupola spouts we recommend the use of manufactured trough tile. They are easy to install, their use saves considerable labor, they prevent run-outs, they last much longer than ordinary fire brick or sand-clay mixtures and they give much cleaner iron.

REFRACTORIES FOR LADLES

28. Ladle refractories are just as important in the efficient operation of a foundry as any other refractory materials, and proper consideration should be given to them. In desulphurizing ladles, regular fire brick fails rapidly due to the corrosive action of soda ash and soda slags on the joints in the lining. We have found that rammed in, plastic refractory linings give the best and most economical service, provided the plastic is made from calcined fire clay instead of ground up brickbats. Brick linings would be satisfactory if a suitable jointing material could be found. Experiments are being conducted on some materials at this time which appear to be promising.

29. Many foundries use brick linings in large ladles handling cupola iron. When these brick are laid in suitable refractory bonding mortar, the life of the brick is lengthened considerably.

30. Silicious ramming refractories are coming into wide use for lining ladles handling cupola iron, malleable iron and electric furnace steel. This type of refractory is especially valuable in lining covered pouring ladles. The material is highly refractory, has low permeability, has high bond strength and is easily rammed into a solid, monolithic structure.

31. Circular fire clay discs, called ladle bottom tile, will prevent bottom run-outs, give a dry bottom, are easy to install and offer a substantial saving in labor cost. They last much longer than 9-in. straights and 1¼-in. splits.

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DISCUSSION

Presiding: L. C. HEWITT, LaClede Christy Clay Products Co., St. Louis, Mo.

DR. E. E. MARBAKER[1]: Are you making any de-aired brick for this job?

MR. BALES: Yes, but we cannot see a lot of difference. The only thing that de-airing does to the brick is to make it denser. Strange as it may seem, it does not appear to make much difference on cupola linings. Cupola block is worn out by the combined action of coke abrasion and erosion from basic oxides, such as iron, lime and magnesia. These oxides react with the brick lining to form part of the slag. The slag itself has some effect upon the cupola lining, but not nearly as much as we used to think, because most of those slags are either calcium-aluminum-silicates or calcium-magnesium-aluminum-silicates.

In studying malleable furnace sidewall brick several yr. ago, we found that the greater the percentage of manganese oxide and iron oxide in the slag, the more corrosive that slag was to the sidewall lining. In cupola operation the slags are rather refractory, and their reaction with the ordinary fire-brick cupola lining is not very corrosive.

DR. MARBAKER: An argument has arisen in the use of firestone. The statement has been made that it is not necessary to have a smooth lining; that it is quite all right if small nubbins of firestone protrude from the lining. As a matter of common sense, it would seem better to eliminate such protrusions, because I can visualize the stock coming down through the shaft and dislodging some pieces which might bring large sections of the lining with them.

MR. BALES: I think you have answered your own question. If those pieces protrude so that they may be knocked into the melting zone, there is no telling what might happen. A piece of scrap may cling to one of those nubbins and divert the blast around the cupola, thereby cutting out one section of the lining. If pieces are knocked off, the siliceous material is necessarily going to require considerably more fluxing to remove it as slag, and, if allowance has not been made in the limestone or soda ash charges, will result in a very inefficient slagging practice. Extra slagging, or the formation of a bridge may result.

MEMBER: What would you consider an unusual size?

MR. BALES: I would say 2- x 2-in., or 1- x 1-in.

DR. MARBAKER: That was the size used. In the argument referred to it was stated that it is quite all right for pieces of firestone to protrude as much as 2-in. It seemed to me that the idea was entirely wrong, and that the lining should be perfectly smooth.

MR. BALES: I think nearly all foundrymen will agree that the smoother the lining in the shaft, the better the job.

MEMBER: Two in. on each side would considerably change the diameter of the lining, and the diameter, consequently, would be reduced 4-in. That would not be right.

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[1] Mellon Institute, Pittsburgh, Pa.

MEMBER: What is your opinion of a cupola lined with fire brick and zone-patched with firestone?

CHAIRMAN BALES: Many operators employ cupola block-lining and probably use small sections of firestone for patching. It is all right.

MEMBER: Are you speaking about true firestone, or are you speaking about sandstone that is so-called firestone?

CHAIRMAN BALES: The so-called firestone is sandstone. There are different types of sandstone, and the people who are in the business refer to it as firestone. There are firestones, or sandstones, in the northern part of Ohio; there are sandstones in Kentucky; and there are sandstones in Tennessee. Those are the three types which are generally used for foundry work, and each of those three has its definite characteristics. The stone in the northern Ohio section is more or less bedded; it is easier split. The stone down south does not seem to have those bedding planes; it seems to be much more refractory and more expensive. My opinion is based upon the findings that foundrymen, who have experimented with those materials, reported in these meetings.

G. S. EVANS[2]: I think it might be well to mention that most so-called "slag" defects in steel castings can be traced to the use of low melting point ladle lining materials and to improperly dried ladles. I know of several cases where such defects disappeared after adopting the new highly siliceous ladle lining material prepared by the author's Company. The initial cost of the lining will run higher than with home-made ganister linings, but the prepared lining usually will last longer as well as prevent slag defects, thus proving cheaper in the end.

As for patching the cupola, I think this should be done with fire brick, which can be anchored so as to prevent the patch from slipping down and causing trouble as soon as the mud softens up with heat. With 6-hr. or longer heats, as now followed in most production foundries, usually it will pay to put in several new courses of brick each day rather than a thick mud patch, which is oftentimes responsible for cold melting at the start of the heat.

MEMBER: I think this is a very excellent paper, and I find that the points brought out are in close agreement with my experience in the operation of cupolas. I have used a lot of firestone in the melting zones. I think it is very good. Brick is good, also. Regarding the smoothness, etc., we have never had firestone so rough that it caused any particular trouble.

As to patching, we do not use firestone. It is always installed in the form of a block. We usually run about 2 days. That is, the first day we would not patch at all, and the second day we would do a good job.

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2 Mathieson Alkali Works, Inc., New York, N. Y.